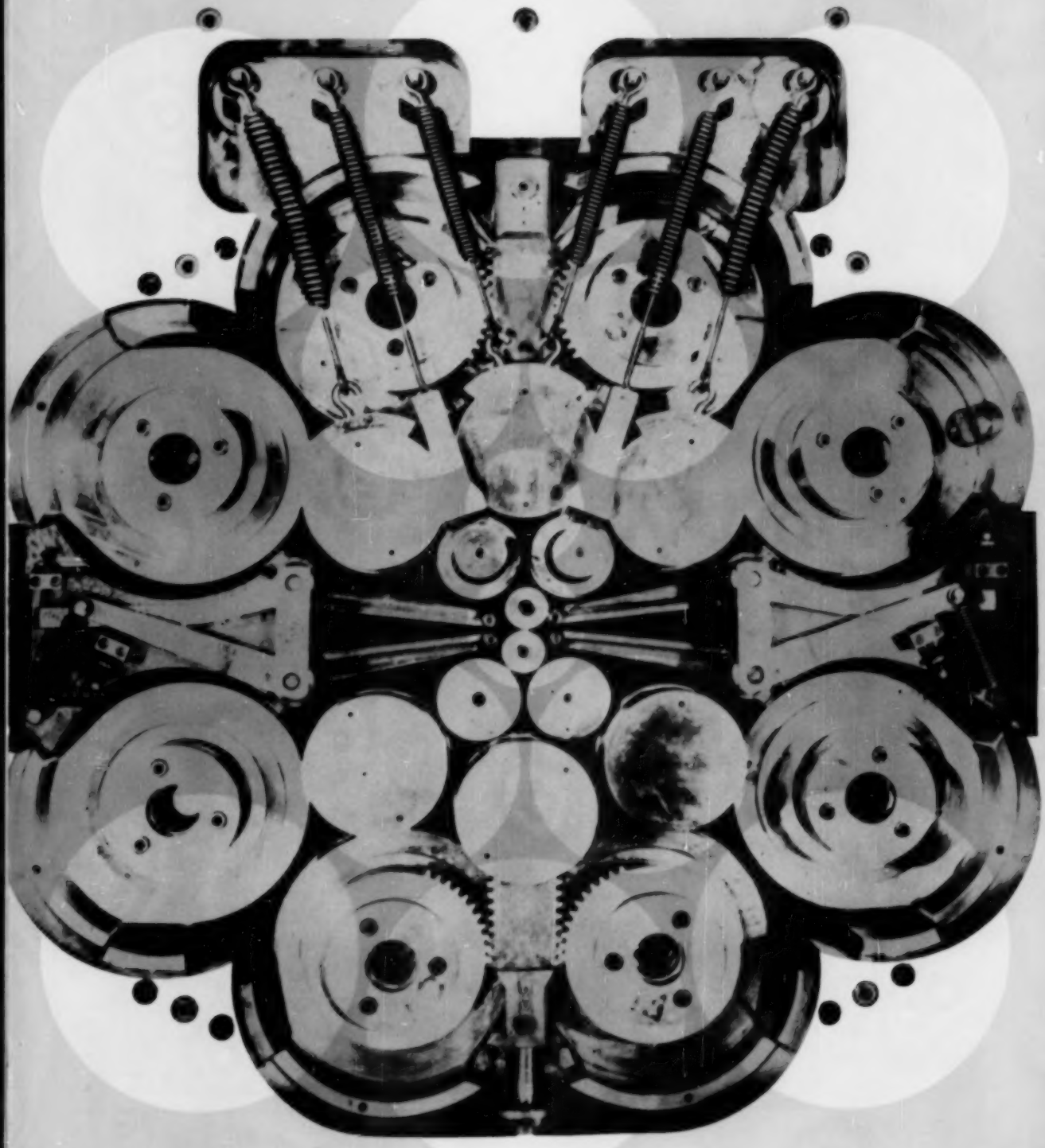
JOURNAL OF

METALS

SEPTEMBER 1956



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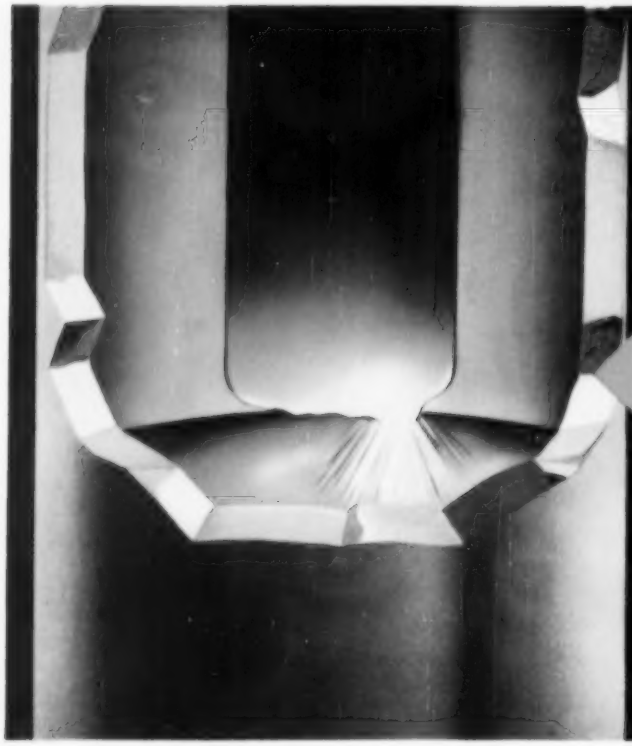

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JOURNAL OF

METALS

VOL. 8, NO. 9

SEPTEMBER 1956

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COVER

Roll design of a Sendzimir cold strip mill shown by Cover Artist Harry Bracke illustrates recent cluster-type mills for cold rolling ferrous and nonferrous strip. These mills are described in the article starting on page 1155. AIME's recently formed Mechanical Working Committee has contributed to general knowledge of steel rolling practice through a review of blooming and slabbing mill practice. A report on this survey appears on page 1154.

FEATURES

Personnel	1122	AIME News	1201
Books	1208	Personals	1209
Reporter	1139	Coming Events	1212
Drift	1145	Advertisers Index	1212

IMD Fall Meeting Program 1202

ARTICLES

Review of Industry-Wide Blooming and Slabbing Mill Practices — W. E. Dittrich 1147

The Sendzimir Cold Strip Mill — M. G. Sendzimir 1154

USSR: Constant Humidity Blast Furnaces 1159

Lignite Coke in Low-Shaft Blast Furnaces — G. Bruckner 1160

Electrolytic Titanium — M. E. Sibert and M. A. Steinberg 1162

TRANSACTIONS

Some Effects of Hot Strip Mill Rolling Temperatures on Properties of Low Carbon Sheet Coils — D. T. Goettge and E. L. Robinson 1169

Factors Affecting Coiling Temperatures in the Hot Strip Mill — J. G. Sibakin and G. M. Ikeda 1174

Magnetite in the Hurley Copper Smelter — H. W. Mossman 1182

Basal Plane Development in Electrodeposited Hexagonal-Close-Packed Metals: Zinc, Titanium, and Zirconium — W. R. Opie 1192

Oxygen in Liquid Iron-Nickel Alloys — H. A. Wriedt and J. Chipman 1195

TECHNICAL NOTE: Exposure of Shrinkage Cavity during the Reheating of Open Top Killed Steel Ingots — L. J. Trilli 1199

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PERSONNEL

THE following employment items are made available to AIME members on a non-profit basis by the Engineering Societies Personnel Service, Inc. (Agency) operating in cooperation with the Four Founder Societies. Local offices of the Personnel Service are at 8 W. 40th St., **New York 18;** 100 Farnsworth Ave., **Detroit;** 57 Post St., **San Francisco;** 84 E. Randolph St., **Chicago 1.** Applicants should address all mail to the proper key numbers in care of the New York office and include 6c in stamps for forwarding and returning application. The applicant agrees, if placed in a position by means of the Service, to pay the placement fee listed by the Service. AIME members may secure a weekly bulletin of positions available for $3.50 a quarter, $12 a year.

MEN AVAILABLE

Metallurgist, master of metallurgical engineering and chemistry, 28. Three years diversified experience in steel mill operations, heat treatment and investment casting, material specifications. Prefer East or West Coast. M-152.

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Metallurgist, 23 to 35, B.S. in chemistry or metallurgy, with approximately two years experience in the light metals field. Will perform metallurgical duties in extrusion and possibly finishing areas. Salary, $6240 to $7920 a year. Location, Maryland. W3804.

Research Supervisor, metallurgist, to perform and supervise research in the field of high temperature materials. Experience should be such that the man chosen could direct the efforts of a small research group. Areas of research include: study and development of high temperature alloys, studies of creep and relaxation behavior, determination of properties, and specifications of materials for high temperature use. Salary from $10,000 a year, depending upon experience. Location, upstate New York. W3802.

Metallurgist, trainee, graduate, 20 to 29, for work in a production environment. Will trouble shoot on technical problems that develop in fabricating aluminum foil products. Must work and cooperate with foremen and supervisors on best methods of fabricating products. Salary, $4800 to $6600 a year. Location, West Coast. W3778.

Metallurgist, for powder metallurgy operation, with five to ten years experience, preferably in general nonferrous metallurgy. Powder metallurgy experience not required. Salary, about $8000 a year. Location, New York metropolitan area. W3609.

(Continued on page 1124)

Additional classified advertising appears on pages 1123, 1124, 1128, and 1132.

METALLURGISTS

Several openings exist in the Metallurgical Departments of our plants in Cleveland, Pittsburgh and Aliquippa, Pa., for metallurgists with the B.S. degree and up to five years experience, preferably in the steel industry.

Opportunity for varied experience, leading to increasing responsibility and earning capacity in metallurgical investigation, customer contact, or operations supervision.

Of particular interest would be experience in production of electric-weld line pipe (Aliquippa) and arc furnace melting (Cleveland).

Openings also for research metallurgists (Ph.D., M.S., or B.S.) at our Pittsburgh laboratories.
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At the country's largest design-engineering center for nuclear power reactors, Bettis Plant in Pittsburgh, operated for the Atomic Energy Commission by Westinghouse, the application of nuclear power has progressed rapidly. However, the nuclear power plants already in operation today represent only the beginning of a new technological era. *Major advances in many areas are necessary.*

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Please submit resume of experience and indicate salary requirements. All replies will be treated with utmost confidence.

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Chicago 16, Illinois

Personnel . . . .

(Continued from page 1122)

Metallurgist, with two to five years experience, for research and process development of new and improved methods for the recovery of uranium from ores. Prefer background in hydrometallurgy or related fields such as mineral dressing and ore dressing. Salary open. Location, vicinity of Boston. W3759.

Metallurgical Engineer, for manufacturing with company engaged in control valves for atomic energy reactors, heat exchangers, etc. Salary open. Company will pay placement fee and moving expenses. Location, Oklahoma. W3735(b).

Staff Consultant, metallurgist, with smelting and refining copper experience. Feasibility reports on all base metals, etc. Salary about $12,000 a year. Location, Ohio. W3728.

Development Engineer, young, chemical, mechanical, metallurgical, or mining, for developmental work on a pyrometallurgical process for beneficiating low grade ores. Should have completed military service or be draft exempt. Salary commensurate with age, experience, and training, to $8000 a year. Company will pay costs of interviews and relocation. Location, Brooklyn. W3723.

Research Metallurgist, B.S., M.S., or Ph.D. in metallurgy, with experience in research in metallurgy, to participate in one or more of the following: 1) alloy development; 2) corrosion control studies; 3) quality control studies; 4) development of manufacturing methods and processes. Salary, $5220 to $9600 a year. Company will pay one half of placement fee when applicant reports for work; the other half after six months employment. Will pay traveling expenses for interviews; moving expenses are negotiable. Location, Pennsylvania. W3695(b).

Metallurgical Engineer, young, graduate, with two to five years steel mill experience for supervisory position in quality control department of integrated steel plant. Salary open. Location, southeastern Pennsylvania. W3381.

(Continued on page 1128)

CHIEF MELTING METALLURGIST

Progressive alloy steel producer, East Central New York location requires Metallurgist for arc furnace melting of stainless steels and super alloys. Prefer college graduate in Metallurgy, Chemical Engineering or Physical Chemistry with minimum 5 years meltshop experience. Salary to $10,000 commensurate with experience. Reply by letter to:

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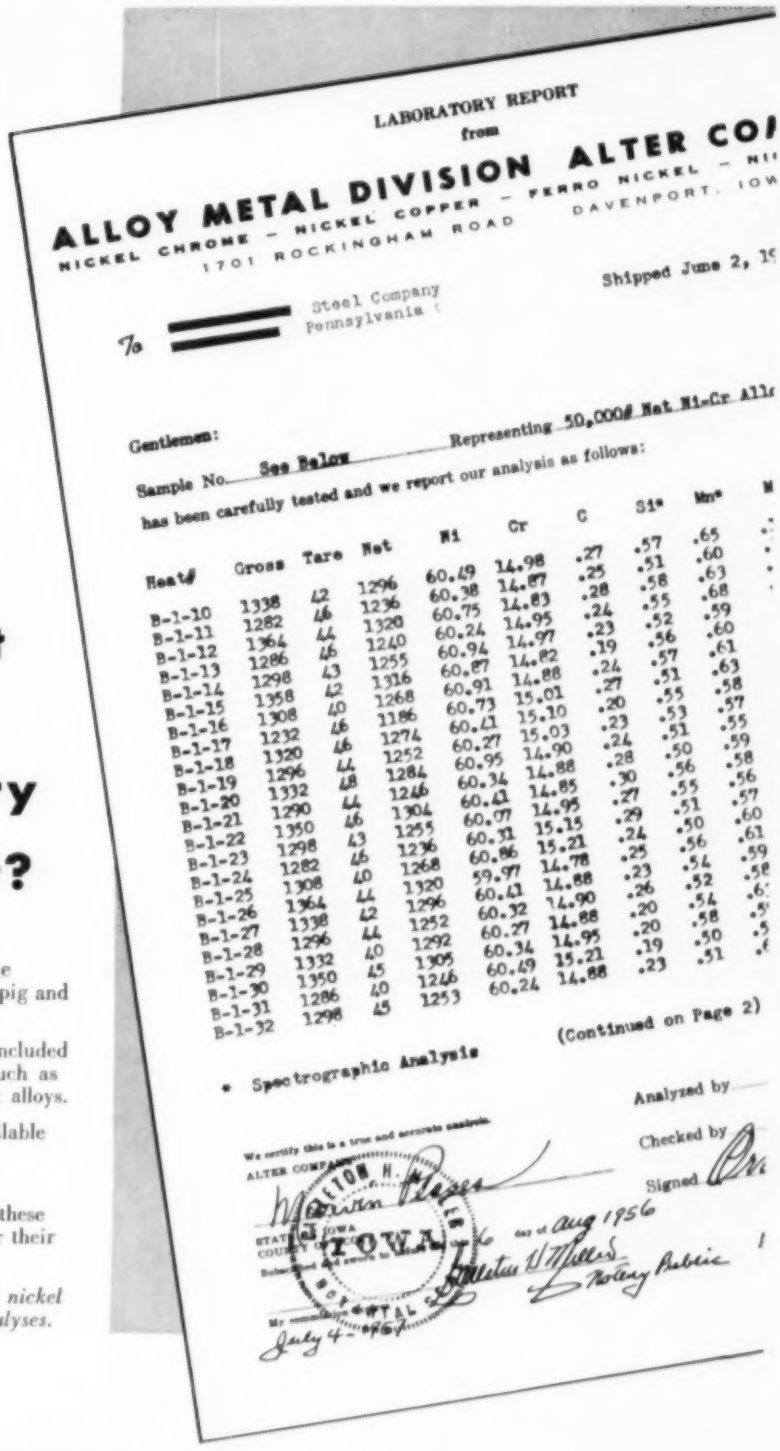

LABORATORY REPORT
from
ALLOY METAL DIVISION ALTER CO
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To Steel Company Pennsylvania

Shipped June 2, 19

Gentlemen:

Sample No. See Below Representing 50,000# Nat Ni-Cr All
has been carefully tested and we report our analysis as follows:

Heat#	Gross	Tare	Net	Ni	Cr	C	Si*	Mn*	M
B-1-10	1338	42	1296	60.49	14.98	.27	.57	.65	
B-1-11	1282	46	1236	60.38	14.87	.25	.51	.60	
B-1-12	1364	44	1320	60.75	14.83	.28	.58	.63	
B-1-13	1286	46	1240	60.24	14.95	.24	.55	.68	
B-1-14	1298	43	1255	60.94	14.97	.23	.52	.59	
B-1-15	1358	42	1316	60.87	14.82	.19	.56	.60	
B-1-16	1308	40	1268	60.91	14.88	.24	.57	.61	
B-1-17	1232	46	1186	60.73	15.01	.27	.51	.63	
B-1-18	1320	46	1274	60.41	15.10	.20	.55	.58	
B-1-19	1296	44	1252	60.27	15.03	.23	.53	.57	
B-1-20	1332	48	1284	60.95	14.90	.24	.51	.55	
B-1-21	1290	44	1246	60.34	14.88	.28	.50	.59	
B-1-22	1350	46	1304	60.43	14.85	.30	.56	.58	
B-1-23	1298	43	1255	60.07	14.95	.27	.55	.56	
B-1-24	1282	46	1236	60.31	15.15	.29	.51	.57	
B-1-25	1308	40	1268	60.86	15.21	.24	.50	.60	
B-1-26	1364	44	1320	59.97	14.78	.25	.56	.61	
B-1-27	1338	42	1296	60.43	14.88	.23	.54	.59	
B-1-28	1296	44	1252	60.32	14.90	.26	.52	.58	
B-1-29	1332	40	1292	60.27	14.88	.20	.54	.6	
B-1-30	1350	45	1305	60.34	14.95	.20	.58	.5	
B-1-31	1286	40	1246	60.49	15.21	.20	.50	.5	
B-1-32	1298	45	1253	60.24	14.88	.19	.51	.6	

(Continued on Page 2)

* Spectrographic Analysis

Analyzed by
Checked by
Signed

We certify this is a true and accurate analysis.
ALTER COMPANY
STATE OF IOWA
COUNTY OF SCOTT
Subscribed and sworn to before me this day of Aug 1956
Notary Public
My commission expires July 4 1957

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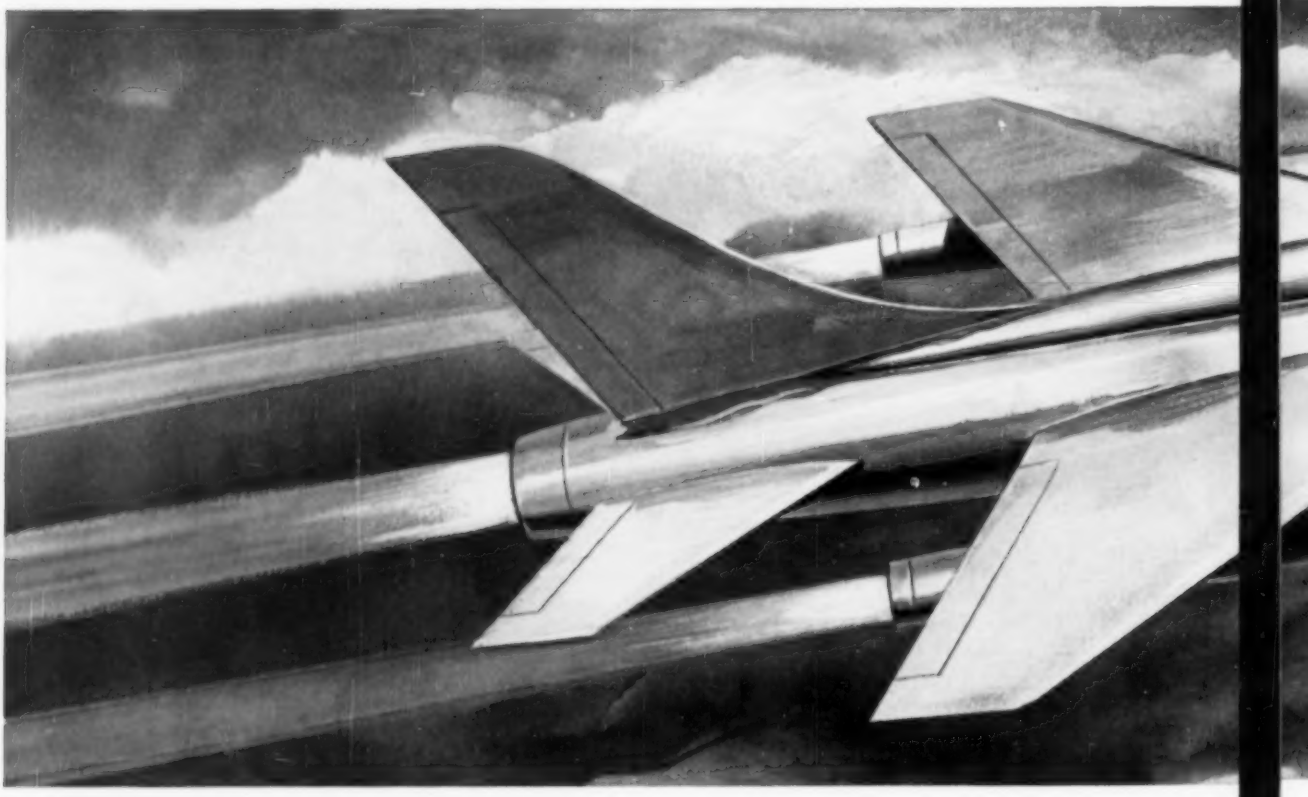
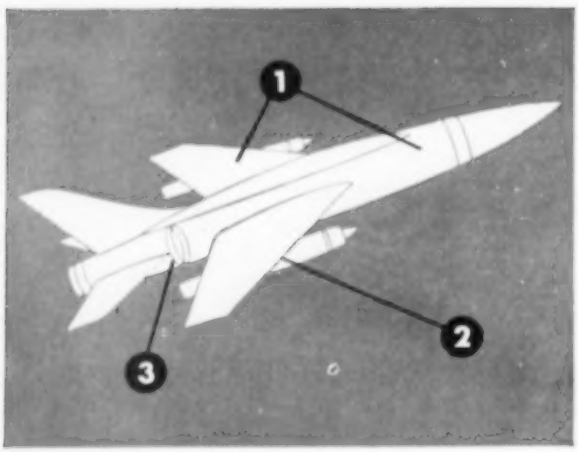
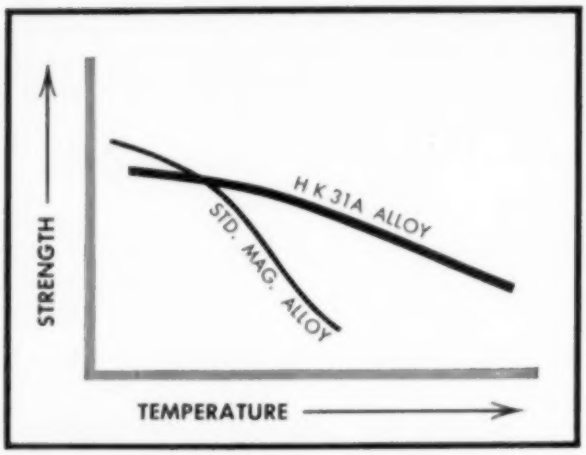
NEW DOW MAGNESIUM ALLOYS





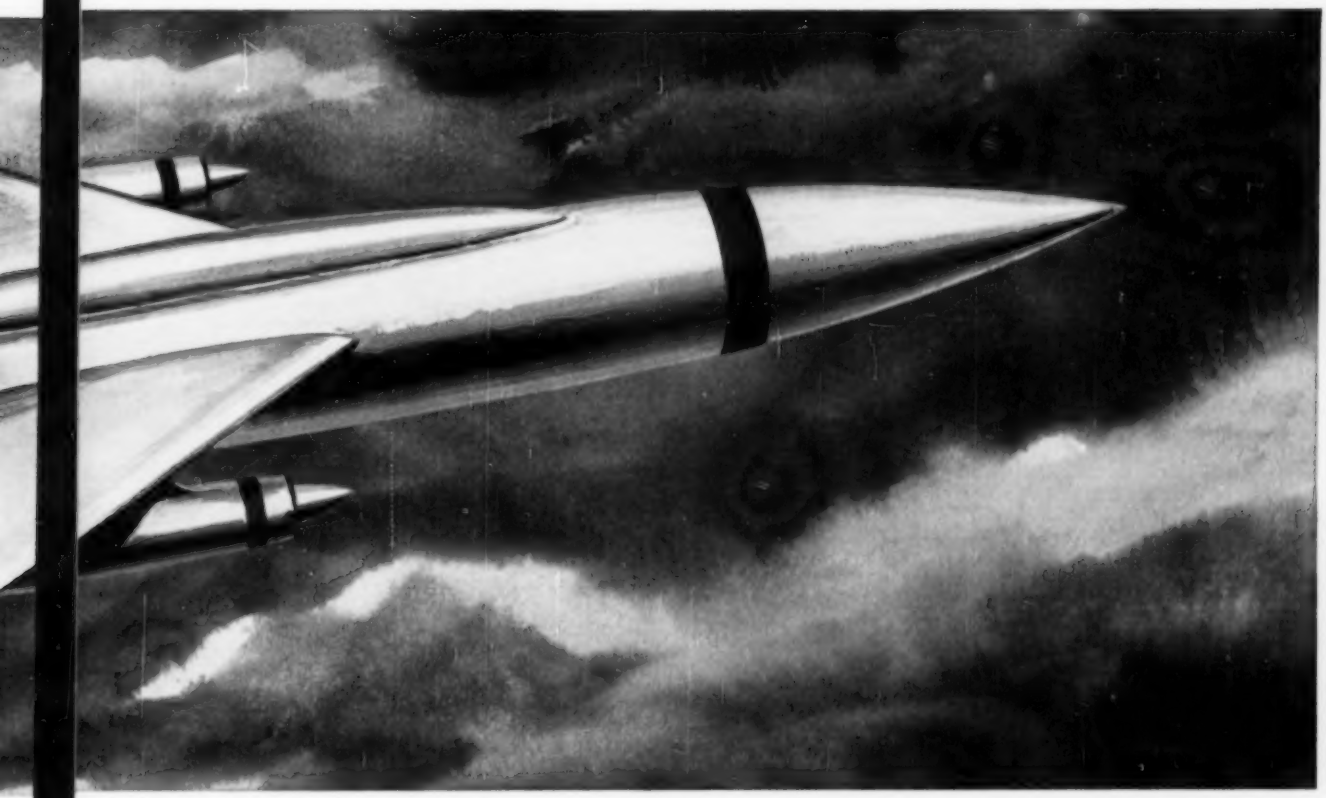
Now suggested *for a broad range of uses in missiles and aircraft, the new Dow magnesium alloys are available in the form of (1) sheet or plate, (2) extrusions, (3) castings.*



Maintenance of strength *at high temperatures is illustrated by this chart. Performance data on the new alloys at elevated temperatures can be obtained by request.*

DOW

CRACK HEAT BARRIER!



High temperature magnesium alloys are available to lighten aircraft and missile structures

Once again the horizons for aircraft structural design have been widened. Dow has developed a series of high temperature magnesium alloys which are already in pre-production use on aircraft, missile and engine structures. These alloys show advantages at temperatures up to 700° F. Limited test data on properties up to 800° F. are available for some of these alloys.

The new alloys save precious pounds because of their good combination of modulus and properties, including creep strength, at temperature. Shop characteristics include good formability and weldability.

One of the available alloys is the magnesium-thorium composition, HK31A, which is manufactured in rolled and cast form. Under development is a similar alloy for extruded shapes and forgings. HK31A sheet and plate are available from stock and from current mill delivery schedules in standard sizes from 0.016″ to 2″.

These new magnesium alloys by Dow should be considered for your high temperature requirements. Contact your nearest Dow sales office or write THE DOW CHEMICAL COMPANY, Magnesium Sales Dept., MA 361E, Midland, Michigan.

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Please send me information pamphlet on services available, how air mail can expedite them, and their cost.

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City .....

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Personnel . . . .

(Continued from page 1124)

Research Metallurgist, M.S. or Ph.D., to 40. Will consider new Ph.D. in physical metallurgy, but with less experience, education will be more of a consideration. Knowledge of heat treating, metallography, X-ray techniques. Will apply research with occasional work of basic nature. Emphasis will be on high temperature properties and titanium. Depending on experience, may be acting as resident engineer or as project supervisor, if qualified. Salary, $5,400 to $10,800 year, depending on experience. Location, Colorado. C4456.

Minerals Engineer, metallurgical degree plus one-year experience in minerals processing, extraction, hydrometallurgy, pyrometallurgy, pilot plant operation, or plant process improvement. Duties will be assignments in any of the foregoing for a research industry. Salary, $5400 to $9600 a year. Location, Ohio. C5148.

Metallurgical Engineer, preferably graduate, to 50, with minimum of four years experience with wire-drawing and strip-rolling equipment and products, for metallurgical engineering work with products manufactured from platinum, gold, silver, and other nonferrous metals. Salary, to $9000 a year. Location, northern New Jersey. W3522.

Metallurgical Engineer, with at least five years specification, processing, heat treating, and plating of ferrous and nonferrous materials for electronic manufacturer. Salary, $8,000 to $12,000 a year. Location, Boston area. W3573.

Metallurgist, for flotation control and research. Salary about $6600 a year. Location, Mexico. F3800(a).

Research Engineer, physics, graduate in electrical, metallurgy, or physics, for basic physical research in metals and electricity related to metals. Will consider man with no experience, through advance degrees, up to three years experience. Salary, from $5000 a year. Location, Pennsylvania. W3761.

(Continued on page 1132)

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XRD-5S SPECTROMETER may be used exclusively for chemical analysis or quickly rearranged as a diffractometer for crystal analysis.

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THE above statement from a large steel foundry reflects the contribution of this versatile General Electric apparatus to analysis programs in chemical, rubber and petroleum plants, as well as in metallurgy.

G.E.'s new X-Ray Emission Spectrometer has proved far faster and more precise than wet methods for quantitative analysis of elements down to atomic number 12 (magnesium). And because the technic is non-destructive, re-runs can be made immediately without preparing another sample.

Whatever your analysis requirements — research, raw materials or quality control — this General Electric diffraction unit can be adapted to your exact needs. Our x-ray representative near you will be glad to recommend the facilities best suited to your work load. Or write to X-Ray Department, General Electric Company, Milwaukee 1, Wisconsin, for Pub. AY-94.

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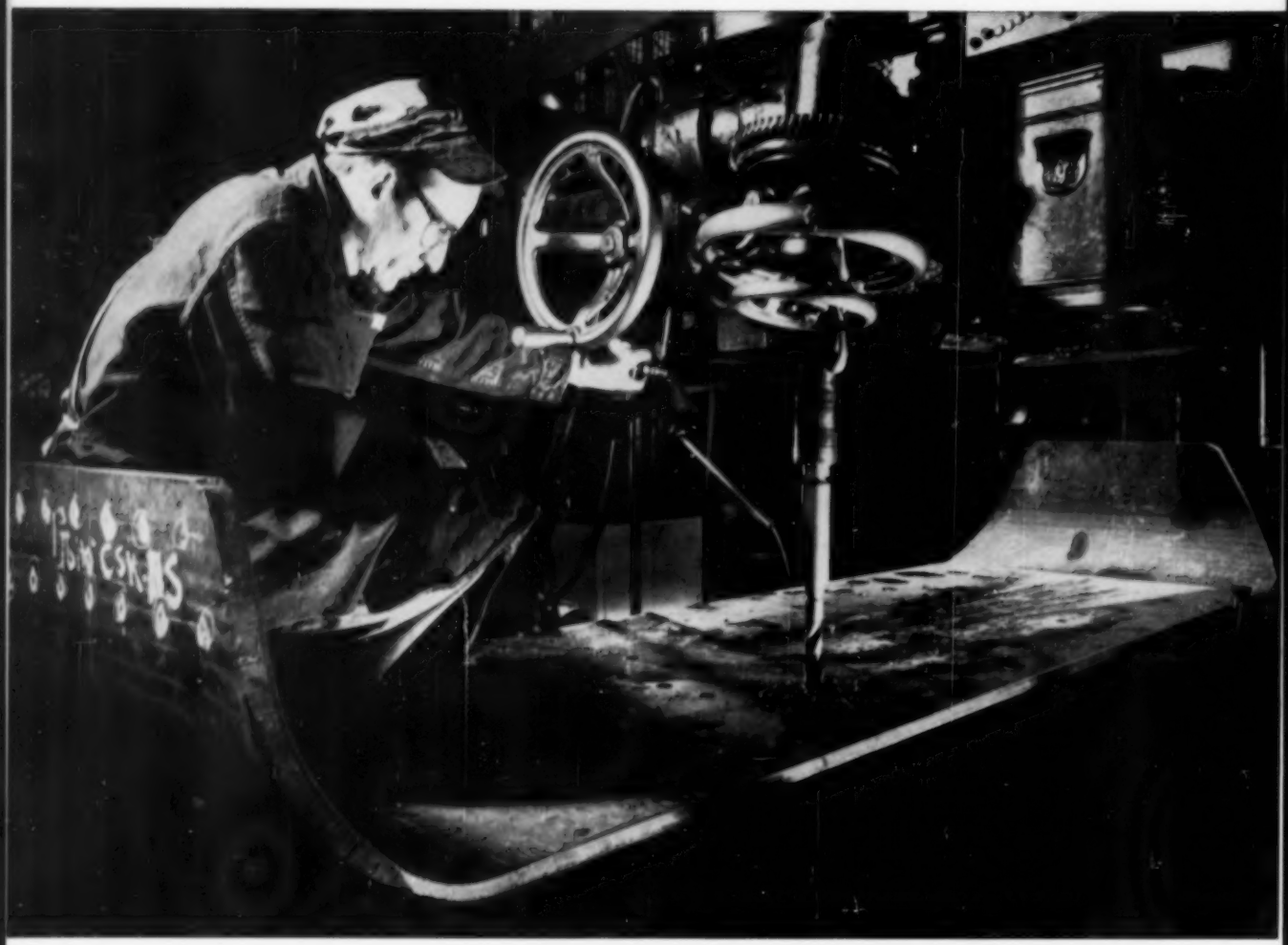
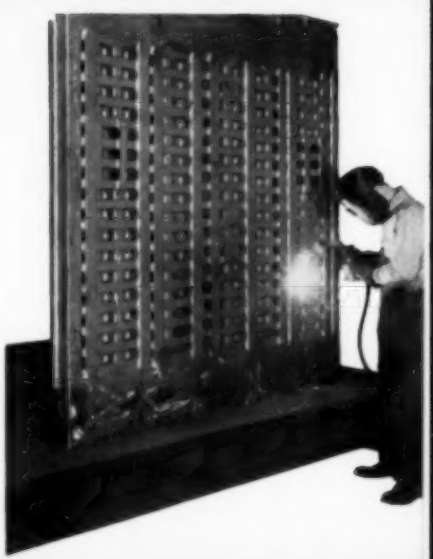
HOW USS "T-1" STEEL IMPROVES THESE PRODUCTS...



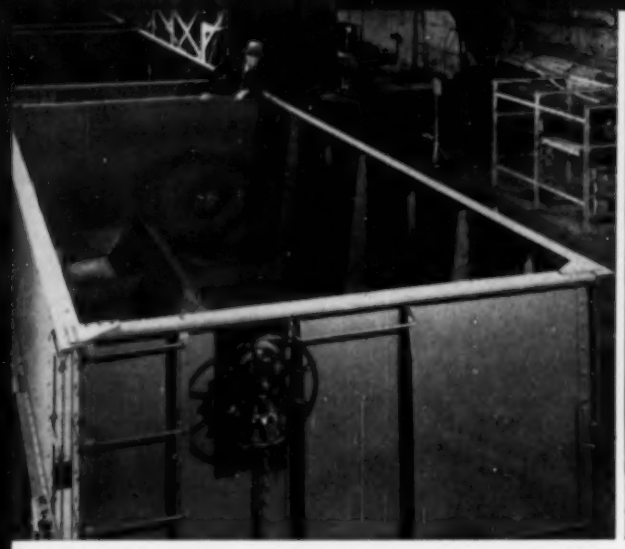
. . . Lops off 1,047 Pounds

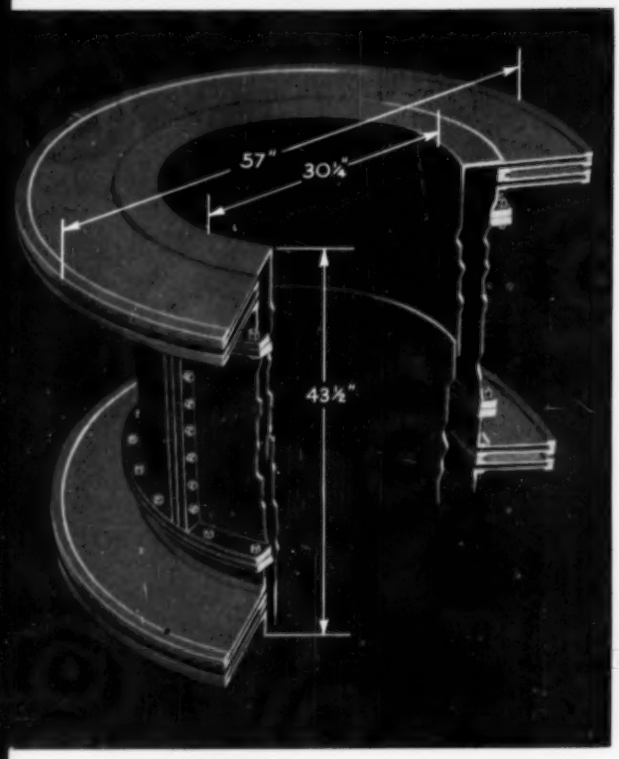
Trays for ore clean-up buckets need tremendous resistance to impact, abuse and abrasion. Blaw-Knox Company, Pittsburgh, Pa., found that they weigh half a ton less, and cost less to fabricate when made from USS "T-1" Steel plate instead of heavy steel castings.



. . . 534 Easier Welds

This printing press bedplate, manufactured by Graver Tank & Manufacturing Co., Inc., must be welded in 534 places. High alloy steel with the needed strength was very difficult to weld. But USS "T-1" Steel is easy to weld . . . and has the needed strength to keep these bedplates, used on high-speed printing presses, as lightweight as possible.





. . . Saves $18,043

In structures like this, which are steel bellows that flex up-and-down and sideways, large thermal expansions can be accommodated. These expansion bellows surround struts which support a large axial flow compressor rotor. The bellows are connected from the compressor stator to the foundation. Westinghouse Electric Corporation switched to USS "T-1" Steel . . . promptly cut not only the cost of machining and welding, but also cost of the steel itself—saved $18,043.

. . . Three Ways Better

International Nickel Company of Canada expects USS "T-1" Steel to increase the service life, to reduce the maintenance, and lower the long-term cost of ore cars like this one; because "T-1" Steel has far greater strength, toughness, and resistance to abrasion than steel used previously. The car builder, Canadian Car and Foundry Co., Ltd., has had no difficulty fabricating this very strong alloy steel.

HOW USS "T-1" STEEL CAN HELP YOU

The great strength and toughness of USS "T-1" Steel (90,000 psi. minimum yield strength) helps you to increase the capacity and durability of power shovel buckets and storage tanks without increasing weight.

Its excellent weldability enables you to fabricate large equipment out in the field without heat treatment . . . thus, reduces fabricating and shipping costs and speeds up construction.

Its unusual toughness at sub-zero temperatures helps you to lengthen service life of equipment that must take impact, abrasion, and abuse in all weather.

USS "T-1" Steel also gives you good creep rupture strength to 900° F. It often can be substituted for more expensive steels that are more difficult to fabricate. There is a place for "T-1" Steel somewhere in your designs. Write, wire, or phone for more information. United States Steel, Room 5408, Pittsburgh 30, Pa.

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USS "T-1" CONSTRUCTIONAL ALLOY STEEL

USS

UNITED STATES STEEL

Personnel . . . .

(Continued from page 1128)

Chief Metallurgist, from 28, with about six years experience in the metallurgical field, preferably in the casting of alnico alloys. Will supervise running of alnico foundry including the analysis of the casting of metals. Salary, $10,000 a year or better, plus profit sharing arrangement. Transportation paid for applicant and family. Location, Brazil. F3580-CS.

Engineers. (a) *Assistant Mill Superintendent,* with a broad background in base metal flotation and a fair amount of mechanical experience and knowledge. Will assist mill superintendent in operating a 1250-ton mill treating a highly complex Cu-Pb-Zn ore, and must be able to take complete charge in absence of mill superintendent. Excellent promotional potential. Salary, $7500 a year. Must be Canadian citizen. (b) *Metallurgist,* qualified to operate 25-ton pilot plant treating complex Cu-Pb-Zn ore. Need good background in base metal flotation research and ability to translate pilot plant results to overall milling problems. Some mechanical ability desired. Salary, $6000 a year. Must be Canadian citizen. Location, Newfoundland. F3552.

Metallurgist, up to 35, for metallurgical control work covering foundry, stainless steel, and spot welding operations for a manufacturer of dairy machinery. Salary, $4800 to $7200 depending on experience. Employer will pay fee. Location, Wisconsin. C5199.

Metallurgist, graduate, 30 to 40, with eight years minimum experience in metal specifications, metal analysis, manufacturing knowledge, heat treating, stress analysis, up-to-date knowledge of latest modern metals. W3501.

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Career opportunities for metallurgical, mechanical, and electrical engineers in the development of new process-metallurgy or industrial-gas applications for steel mill and foundry usage.

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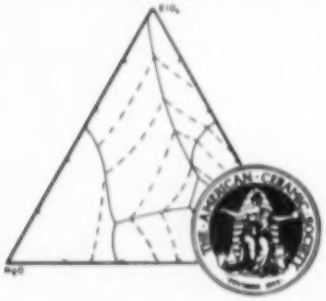
Phase Diagrams for Ceramists. ERNEST M. LEVIN, HOWARD F. McMURDIE, AND F. P. HALL. Edited and published by the American Ceramic Society, Inc., Columbus 14, Ohio, 1956. 286 pp. $10—This enlarged and revised edition contains eight hundred and eleven phase diagrams covering (a) metal oxide systems, (b) systems containing nonmetal oxides, (c) systems containing halides, sulfides, etc., and (d) water-containing systems. The general discussion includes (1) an enlarged glossary of about 50 terms; (2) a comparison of the three important temperature scales, in degrees centigrade, that have been used since 1914; (3) methods for determining the composition of phase assemblages; (4) interpretation and representation of multicomponent systems; and (5) a selected and annotated bibliography on the various aspects of phase research covering theory, interpretation, method and techniques, mathematical treatment, thermodynamic calculations, silicate chemistry, and phase diagrams in related fields. Complete author and system indexes.



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Refractories Bibliography

Enamel Bibliography and Abstracts

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Pittsburgh research laboratory of large specialty steel manufacturer offers unusual opportunity for qualified tool and die steel metallurgist. Position provides attractive income and benefits with an opportunity for professional development and early advancement.

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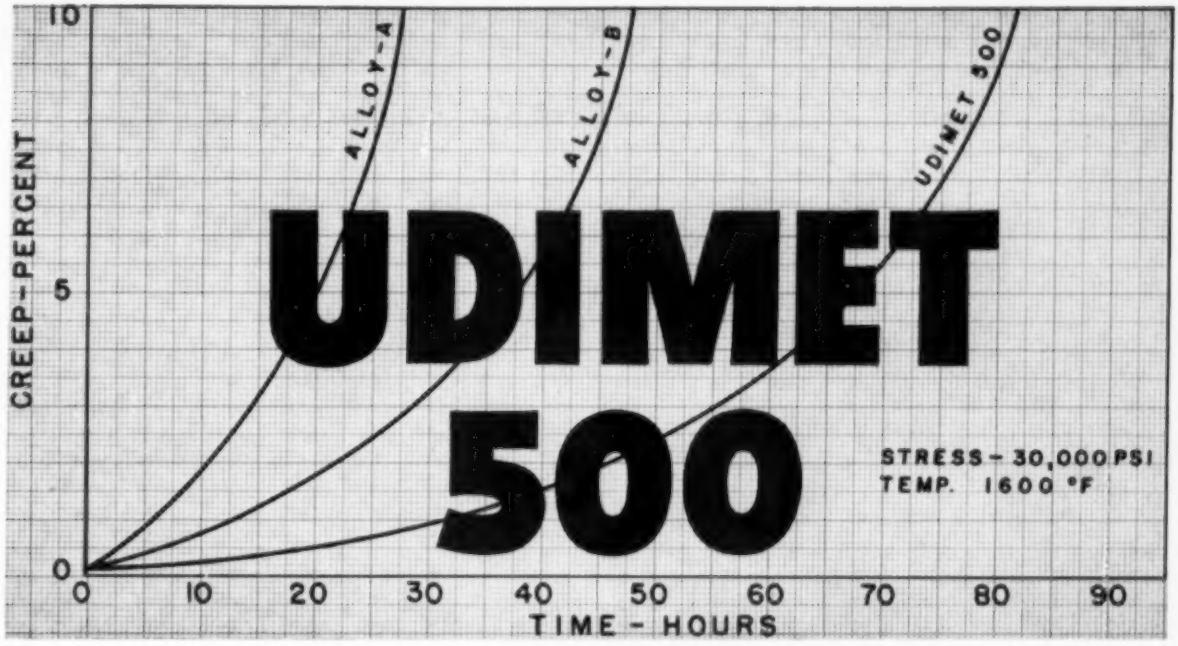

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Here's a new "super alloy" for gas turbine components. UDIMET 500 combines unsurpassed stress-rupture strength with superior high tensile strength in the 1200°F to 1800°F range. For example, at 1600°F its ultimate tensile strength is over 100,000 PSI.

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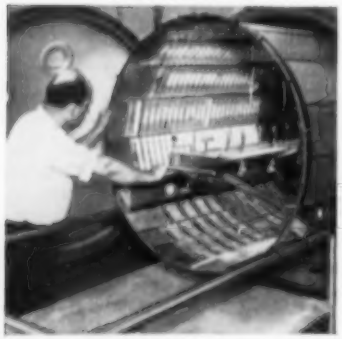
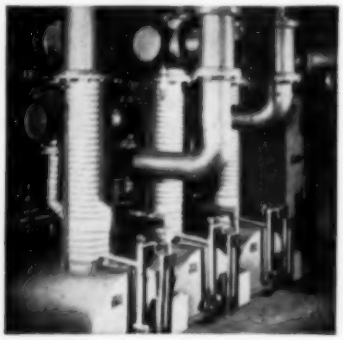
Incorporated within vacuum chamber is a rail-mounted ingot mold carriage, which passes through the vacuum lock, moves into position to receive the heat of processed metal and then carries it out on to the mold storage floor. Vertically-hinged door swings fully open, permitting unobstructed access to interior of furnace.

The metals industry is profiting from the use of other STOKES production equipment



Vacuum Metallizers. Stokes offers a complete line of vacuum metallizing equipment to plate metals for improved surface finish . . . also to provide conductive coatings on non-conductive materials.

Powder Metal Presses. Cost savings and the compacting of incompatible alloys have made powder metallurgy an accepted process. Stokes offers the most complete line of presses in the industry.



Vacuum Pumps. For rapid evacuation and holding high vacuums, Stokes offers mechanical pumps, and "Ring-Jet" oil vapor diffusion and booster pumps. New design affords 10 to 100% greater speeds.

Stokes vacuum furnace installed by Allegheny Ludlum for high alloy steel melting

Research studies with new unit will precede production operations on semi-continuous basis

THIS new Stokes high-vacuum furnace was recently installed at the Watervliet, N. Y. plant of Allegheny Ludlum Steel Corporation. It is an important component of the company's new Induction Vacuum Department . . . which, for the immediate future, will devote itself to various aspects of a development program on a wide variety of alloy steels.

Featuring a particularly compact and simplified design . . . the unit permits the direct flow of materials through the chamber, eliminates the need for costly auxiliary equipment, and minimizes the space required for operations. Controls for vacuum pumping, charging, alloying, melting, and casting are centralized and conveniently located.

If you are planning to explore the interesting potential of modern vacuum metallurgy, plan, too, to take advantage of the undisputed leadership in equipment and experience which Stokes can apply to your problem. Prime supplier of vacuum furnaces for production use, Stokes incorporates the features you need for simplified operation and dependable service. With the Stokes vacuum lock, you can make multiple melts and manipulate the melt without breaking the vacuum. High capacity pumping systems, combining new Stokes Ring-Jet Booster Pumps and rugged Microvac roughing pumps, provide fast evacuation and dependable holding of desired vacuum.

A Stokes engineer will be glad to consult on your specific application, help you select the most suitable of the many basic Stokes designs for your work, and engineer modifications to your special requirements. For technical data, write for Stokes Catalog No. 790, "High Vacuum Furnaces."

Reference Data:

Microvac Pumps—Catalog 750
Diffusion and Booster Pumps
Specification and Performance Data
Story of the Ring-Jet Pump
How to Care for Your Vacuum Pump—
Booklet 755
Vacuum Furnaces—Catalog 790
Vacuum Metallizing—Catalog 780
Vacuum Calculator Slide Rule
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Powder Metal Presses—Catalog 810

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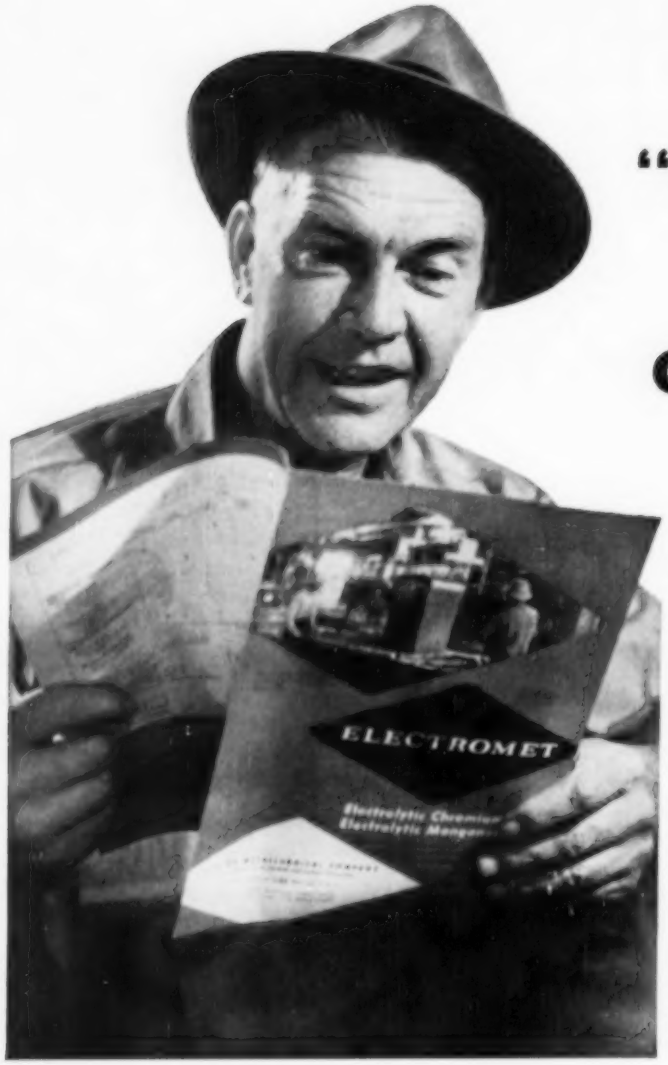

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And ELECTROMET'S booklets can also help you – the metallurgist, the melter, the designer and fabricator, the engineer. Here's a wealth of valuable data, graphs, and technical information on stainless steel...all from ELECTROMET'S extensive technical library. Some booklets discuss the new chromium-nickel-manganese steels. Others deal with the decarburizing and reducing cycles of stainless steel melting.

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New Stainless Steels

4. **NEW STAINLESS STEELS.** High-manganese, extra low-carbon, wrought precipitation-hardenable, and cast precipitation-hardenable grades. Circle F-20,024.

Melting Practices

5. **OBSERVATIONS OF STAINLESS STEEL MELTING PRACTICES.** Circle F-20,006.
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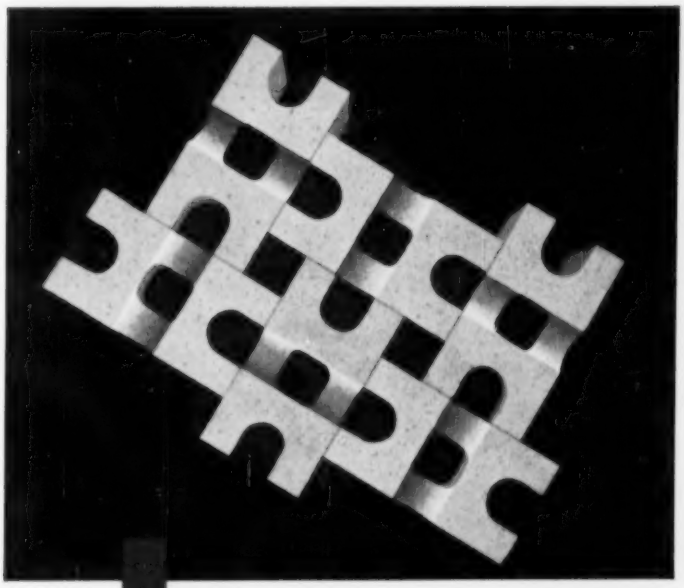
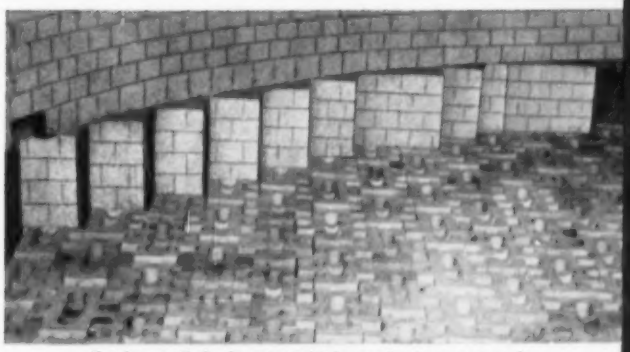
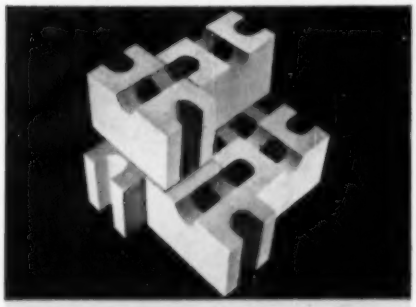


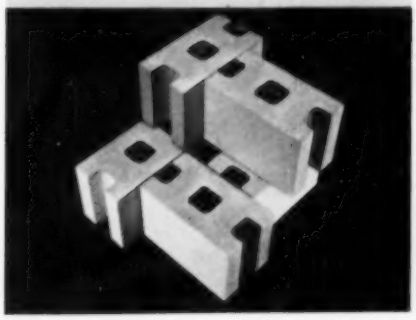
KENNEDY BLAST FURNACE STOVE CHECKERS



Tendency of checkers to twist in service is overcome by pilaster wall construction of the Bailey Hot Blast Stove.



The regular Kennedy Checker (above) is of 3-hole design with unobstructed flue openings, 1¼" minimum wall thickness and a cross flue. It also can be furnished (below) without the cross flue feature.



This new 3-hole checker shape is laid in basket weave style to produce a solid 1¼" wall between each flue. This assures greatly increased heating surface without sacrificing the advantages of basket weave design.

The increased heating surface of the Kennedy Checker results in a correspondingly lower stack temperature. This makes possible the use of a modern steel bottom for supporting the checker system.

Write for Bulletin

WILLIAM M. **Bailey** COMPANY

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Designed with stainless steel to protect product purity

America's largest winery has specified stainless steel in new equipment, as a regular policy to replace old and obsolete installations known to be sources of metal pick-up. This user, The Roma Wine Company, finds prevention far better than the cure. And less costly, too!

Resist Corrosion

Chromium-nickel stainless steels resist attack by chemicals and atmospheres over wide ranges of concentrations, pressures and temperatures.

Prevents Contamination

The inherently hard, smooth surfaces of these austenitic chromium-nickel steels make poor harbors for bacteria or other contaminants.

Easy to Clean

Also, users find stainless steel equipment easy to clean and to *keep hygienically* clean as well as attractive in appearance.

New Stainless Steel Booklet Available

Write for a copy of "Stainless Steel in Product Design." Yours for the asking . . . it tells where and how to use stainless, how to design for lower cost, and other ways to give your products an edge on competition. Write for it now.

Roma Wine Company, a division of Schenley Industries, Inc., makes grape juice concentrate at the rate of 600 gallons per hour in the vacuum pans (above) of chromium-nickel stainless steel. Typical of other stainless equipment used for safeguarding purity of Roma products are pasteurizers, heat exchangers, centrifugal pumps, agitator tanks and wine coolers. All of austenitic chromium-nickel stainless steel.

INCO **THE INTERNATIONAL NICKEL COMPANY, INC.** 67 Wall Street New York 5, N.Y.

Titanium and Zirconium Horizons

Horizons Inc. of Cleveland, an industrial research organization and one of the pioneers in the electrolytic production of zirconium, titanium, and similar metals, has been engaged by Kennecott Copper Co. as consultant in the design, erection, and operation of a pilot plant to produce zirconium in granular form in the Cleveland area. Kennecott has acquired licenses for electrolytic production of zirconium, as well as options on licenses for electrolytic production of titanium, thorium, columbium, and tantalum from Horizons Titanium Corp. . . . Horizons Inc. has also been awarded a $200,000 contract by the U. S. Navy Bureau of Aeronautics for further exploration of these electrolytic processes for titanium. For a review of such techniques see page 1160.

Steel Expansion Highlights Blooming and Slabbing Mills

With continued world-wide expansion of steel producing facilities, makers of rolling mills have been striving to keep up with orders for more than ten years. United Engineering & Foundry Co. of Pittsburgh has announced receipt of an order from Sharon Steel Corp. of Sharon, Pa. for a 44x104 in., two-high blooming and slabbing mill, and an order from The Steel Co. of Canada Ltd. at Hamilton, Ontario for a 46x100 in. two-high slabbing mill. The first mill can roll 6-ton ingots into blooms, slabs, and billets, while the latter can roll 30-ton ingots into slabs up to 72-in. width. . . . In Mexico, one of the country's largest Mills, the Cia. Fundidora de Fierro y Acero de Monterrey S.A. has been authorized an Export-Import Bank loan for $26 million to enable the firm to purchase in the U. S. a 46-in. blooming and slabbing mill, a flat products mill, and various furnaces and equipment. Plans were developed through studies by the Freyn Engineering Dept. of Koppers Co. and by Arthur G. McKee & Co. Rolling mill assistance was furnished by Blaw-Knox Co. This expansion will help Mexico meet its increasing demand for flat rolled steel.

Tool Steel Extrusion Process

Allegheny Ludlum Steel Corp. has successfully hot-extruded tool and high speed steel in a variety of shapes up to a maximum size that can be inscribed in a 4-in. diam circle. The adaptation of the hot extrusion process offers many advantages, for the design permits great flexibility, die costs are small, and new dies can be prepared on relatively short notice. The hot worked extruded structure is similar to rolled material, and very little decarburization has been found.

Taconite Unit Formally Opened

The world's first large-scale taconite plant was formally opened in August at Silver Bay, Minn. The $190 million plant of Reserve Mining Co. is designed to produce 3¾ million tons of iron ore pellets annually for Armco Steel Corp. and Republic Steel Corp., joint owners of Reserve. The opening climaxed the result of 40 years of research and opens a vast new domestic source of high grade iron in the form of pellets of magnetic taconite, a rock once considered worthless.

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reports on

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Coatings of "Solution Ceramics" provide a thin insulating film relatively inert and highly refractory with excellent resistance to thermal shock.

They are obtained when thermal decomposition of atomized ceramic solutions is completed just as the residual particles strike the heated surface to be coated. Methods used depend upon the shape of the surfaces, its heat capacity and thermal conductivity. These processes were developed by the technical staff of the Armour Research Foundation of Illinois Institute of Technology.

RESULTS TO DATE

Field development work, as per agreement with the Foundation, is being conducted by the field engineering staff of TAM. Industrial trials, to the present time, indicate the most likely and advantageous uses in the fields of electrical insulation, inert casting, linings for cartridge cases and as a plate surface for lithographic reproduction.

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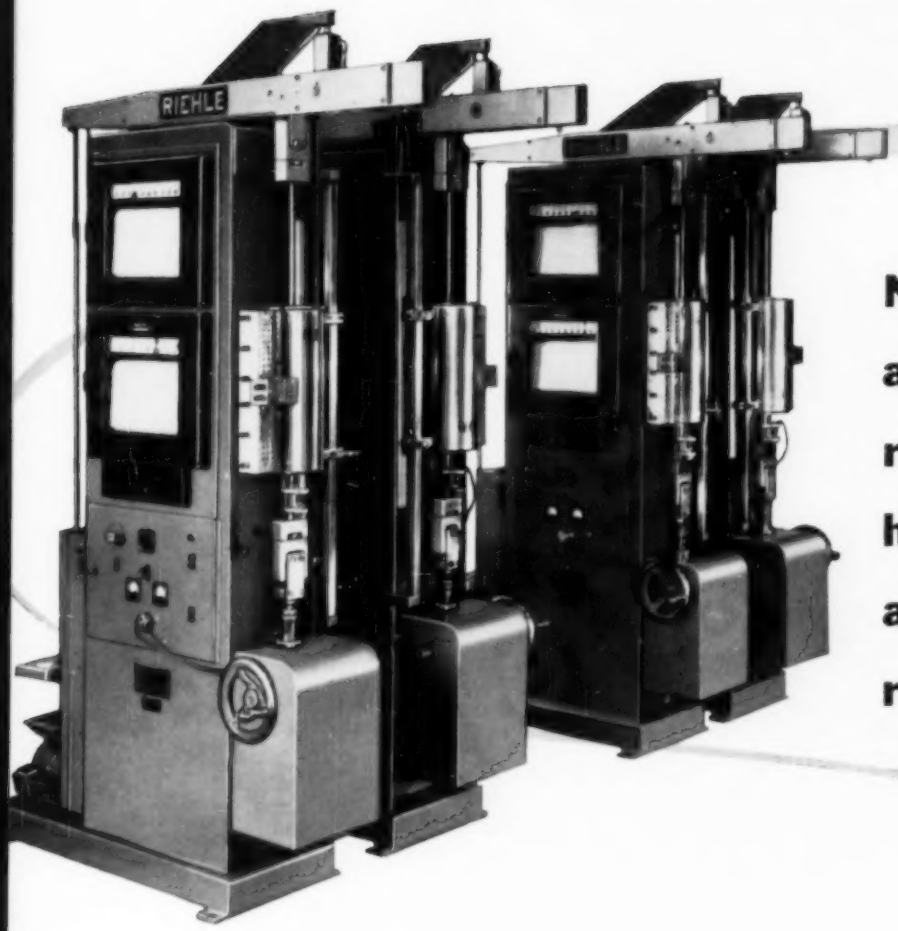

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The Riehle Creep and Stress-Rupture Testing Machine is usually furnished with temperature controller, furnace, local wiring and other necessary components — fully ready to operate. Its design can accommodate a wide range of accessories and instrumentation to insure maximum versatility. This machine is the product of the very best in modern engineering technology and fabricating practice.

NEW 8-page bulletin describes "package" testing machine... instrumentation ... accessories

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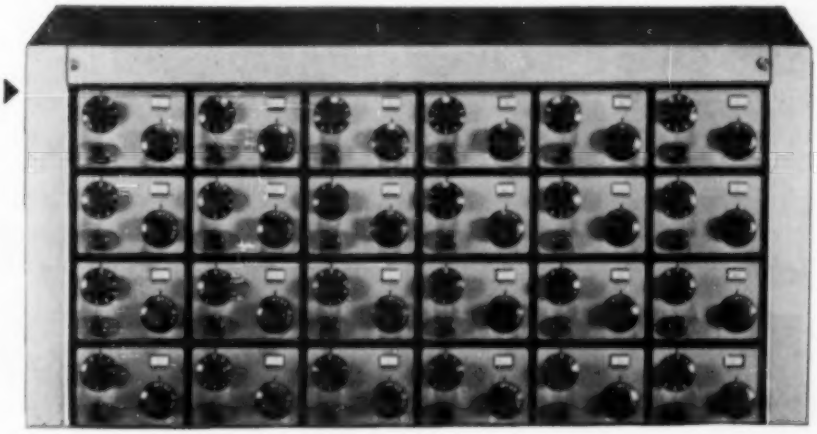

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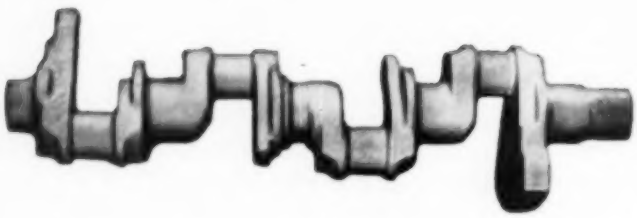

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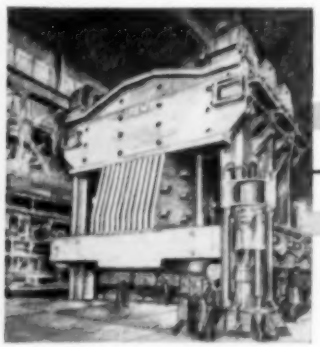

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DRIFT

'A Little Knowledge is a Dangerous Thing'

PERHAPS it is not twisting the meaning of this well turned phrase too much if, rather than applying it to an individual, we apply it to the problem of technical manpower. During the last five years or so, we have all felt the growing seriousness of the engineer shortage, whether it be by having to leave work to go on a tour of college campuses interviewing the ever more choosey graduating engineer, or by trying to expand plant facilities with an already overworked engineering staff of unchanging size.

To those directly concerned with hiring the nation's graduating metallurgists, the problem seems especially tough. Enrollments have not shown any increase, and the only solution seems to be to go out and campaign harder for the existing supply. Little reflection is necessary to realize that this is but another form of inflation—a text book example of supply and demand—and really no solution at all. No, the problem is certainly too large for any firm, or even any industry or professional society, to handle alone. Not that individual persons or firms do not want to help—but direction and wise counsel are needed, and there must be a definite plan.

There is such direction and wise counsel in the form of a group that you have more than likely heard of; it's often called EMC of EJC, and you'll probably hear considerably more of it in the coming months. EMC of EJC stands for Engineering Manpower Commission of the Engineer's Joint Council. It was founded in 1950 as a joint effort of several professional engineering societies, including the AIME. EMC has worked closely with other groups of similar nature, but in the realm of engineers and engineering problems, it is the primary organization.

EMC has three major goals: to demonstrate to the country the growing importance of engineering to economic development and national security; to promote effective utilization of engineers; and to encourage qualified students into our engineering schools.

Articles concerning the engineer shortage that you have read were probably prepared with the help of EMC information. Since 1951, EMC has been making annual surveys, checking the number of engineering degrees granted and the number of jobs available. Salary studies and reports of professional employment conditions have also been made. The fact that the salary scale of engineers in the federal government has been raised twice in three years speaks for the effectiveness of these reports.

For several years following the enactment of Universal Military Training, the greatest waste of our technical manpower was in the draft program, where qualified engineers were driving trucks and cleaning rifles. After much EMC agitation, the Critical Skills Law now allows many of these men to serve for the brief span of six months, and then return to their critical and defense supporting jobs. Progress is also being made toward the establishment of an Army Scientific Corps, along the same line as the Medical Corps, to insure full utilization of engineers and scientists in the army. Indeed, there must always be a foot soldier, but the complexities of military technology demand men to push slide-rules rather than shovels to run the multi-billion dollar mechanisms of defense.

After the first five years of operation, EMC can be proud of its record. However, there are two fields, both of vital interest to all AIME members, in which results have been negligible. The first is enrollments. Although enrollment in many engineering fields during the past few years has been encouraging, metallurgy has not been one of these fields. With the increasing demand for chemical engineers and physicists, industry can no longer expect substantial numbers of these men to "drift" into metallurgy. This is a very serious problem, and it is hoped that through continuing efforts of the EMC, the firms of the metal industry, the newly formed President's National Committee for the Development of Scientists and Engineers, and the universities and colleges themselves, that this situation can be helped.

The second point is the utilization of engineers by private industry itself. A recent EJC report on professional standards and employment conditions emphasizes the importance of this aspect. It is a difficult job for any group to go to the leaders of industry and ask them to make an "agonizing reappraisal," to see if their professional employees are being used to their fullest capacity. Yet, statistics show that most engineers leave their jobs because they don't feel that they're doing what they were trained for, or that they were not getting recognition for that work. The job turnover figures for engineers less than three years out of college (or the army) are truly shocking.

America is currently undergoing an acceleration of technological advancement unparalleled since the very beginning of the industrial revolution. With hardly more than a scratch made upon the potential applications of atomic energy, one might imagine we were entering upon the very steep portion of an exponential curve. To meet these demands, a great number of men and women will be needed who are not only technically competent, but who are also alert, cognitive citizens. It is through such organizations as the President's Committee and the Engineering Manpower Commission that this need shall be met.

J. J. Burke

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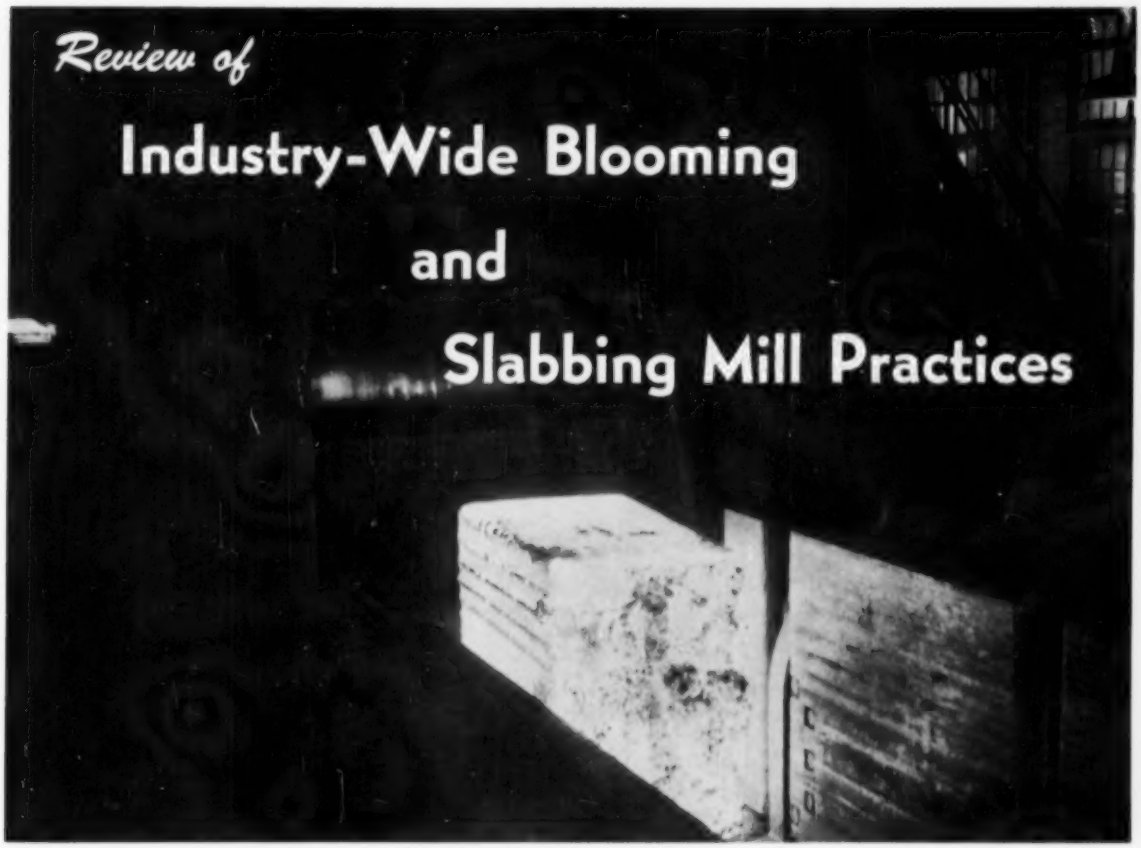
Review of

Industry-Wide Blooming and Slabbing Mill Practices



by W. E. Dittrich, who is Supt. of the No. 2 Blooming, Rail and Structural Mills at Inland Steel Co. The paper on which this article is based was prepared under the auspices of the AIME Mechanical Working Committee, and presented at the New York Annual Meeting 1956.

Frontispiece: 44-in. blooming mill, Gary Works, U. S. Steel Corp.

A Mechanical Working Committee of the AIME was formed in February 1955 for the purpose of sponsoring technical activity in hot and cold working of steel from ingot to finished product. An industry-wide inquiry into primary rolling mills and practices was considered to be an appropriate beginning project.

Most valuable assistance in the preparation and summation of the data has been had from the following people: M. Tenenbaum, Inland Steel Co.; P. H. Smith, LaSalle Steel Co.; and L. J. Trilli, Inland Steel Co. The assistance and cooperation of the Mechanical Working committee is also acknowledged. Committee membership includes: M. Tenenbaum, Inland Steel Co., chairman; R. D. Hindson, Steel Co. of Canada; E. L. Robinson, U. S. Steel Corp.; L. J. Wright, U. S. Steel Corp., Edgar Thomson Works; E. A. Gietzen, Bethlehem Steel Co., Lackawanna Plant; H. B. Emerick, Jones & Laughlin Steel Co.; and W. E. Dittrich, Inland Steel Co.

BLOOMING mill operations have been most important in the expansion of steel plant capacities, and it would follow that a review of blooming facilities and practices might be of special interest at this time. Background data required for the report was obtained from a widely circulated *Questionnaire on Blooming and Slabbing Mill Practice*.

The questionnaire was submitted to all major steel companies with the understanding that data furnished would be confidential and there would be no specific plant references. The response was most gratifying. Replies from 18 companies, giving data for 79 mills, were received; this represents approximately 84 pct of the total capacity of the U. S. and Canada.

The purpose of this review is to give a broad report of the range and variance in facilities and practices. No attempt is made to evaluate the equipment or practices as to metallurgical or operational propriety. It is rather hoped that the report will enable those interested to compare their practices with the general practice of the industry. It is the intention of the committee to offer all of the detail data in monograph form at a later date.

Categories of Primary Rolling Mills

For convenience and simplification the mills have been divided into four general categories:

A) Universal Slabbing Mills,
B) Blooming Mills Rolling Slabs Only,
C) Blooming Mills Rolling Blooms and Slabs,
D) Blooming Mills Rolling Blooms Only.

The questionnaire records ten mills in Group A, seven in Group B, 46 in Group C, and 16 in Group D.

The information requested might be categorized into five areas: 1) mill and roll statistics, 2) rolling regulations, 3) rolling temperatures, 4) ingot and slab sizes, and 5) drafting practices. The questionnaire requested typical drafting practices for well-heated steel. Certain conventions for reporting type and amount of drafting were suggested.

General Data Common To All Groups, Table I

Motive Power: Eighty pct of the mills have electric drives, while 20 pct use steam drives. The highest fraction of steam drives is in Group D—Blooming Mills Rolling Blooms Only.

Limitation of Drafting by Mill Horsepower: Forty-three pct of the reporting mills state that drafts are limited by the mill drive horsepower. The drives considered to be marginal or limiting are greatest in the older Group D mills, while Group A—Universal Slabbing Mills are, in the main, considered by their operators to be adequately powered.

Ragging Practice: About 75 pct of the reporting mills use ragging on the flat or bullhead passes, as well as on edging passes. The least use of ragging is at the universal slabbing mills. Fifty-three pct prefer machine cut ragging and 22 pct knurling. The narrower edging grooves are usually not ragged.

Use of Drafting Schedules and Recording of Pass Practice: Fifty-eight pct report that no drafting schedule is specified in normal rolling practice and that the roller's judgment is the basis of control. Where a drafting schedule is specified, checks for compliance are made in 70 pct of the cases by the metallurgical observer or the mill supervision, 28 pct make no check, and one mill has a pass and draft recorder. Thirty-one pct of the mills have metallurgical observers recording drafting practice.

Tandem Rolling: The only mills showing appreciable tandem rolling are the Group B mills rolling slabs only. The practice is most beneficial to this group since extreme screw travel in edging wide slabs is minimized. The grades usually restricted from this practice are stainless, bottle top and hot top steels and fully killed open top steels. Only 16 pct of the reporting mills use the practice.

Table I. General Data Common to all Four Groups of Primary Rolling Mills

	Group A, Universal Slabbing Mills Rolling Slabs Only	Group B, High-Lift Blooming Mills Rolling Slabs Only	Group C, Blooming Mills Rolling Blooms & Slabs	Group D, Blooming Mills Rolling Blooms Only	All Groups
	Pct	Pct	Pct	Pct	Pct
Motive Power					
Steam	10.0	0.0	21.7	31.3	20
Electric	90.0	100.0	78.3	68.7	80
Motor hp Limiting on drafting practice?					
Yes	20.0	42.9	45.7	50.0	43
No	80.0	57.1	54.3	50.0	57
Roll Ragging Bullhead					
None	50.0	42.8	21.7	6.25	24
Knurled	30.0	28.6	17.4	25.0	22
Machine Cut	20.0	28.6	60.9	62.5	53
Hand Cut	0.0	0.0	0.0	6.25	1
Edges					
None	80.0	14.3	13.1	18.75	23
Knurled	10.0	57.1	23.9	25.0	25
Machine Cut	10.0	14.3	54.3	43.75	43
Hand Cut	0.0	14.3	8.7	12.5	9
Do you specify a drafting schedule to be followed on normal rolling operations?					
Yes	50.0	28.5	26.1	38.5	42
No	50.0	71.5	73.9	61.5	58
If specified, what check is made on the schedule being followed?					
None	40.0	0.0	25.0	37.5	28
Mill Supervision	20.0	50.0	50.0	25.0	42
Metallurgical Observer	20.0	50.0	25.0	37.5	28
Pass and Draft Recorder	20.0	0.0	0.0	0.0	2
If not specified, what is basis on drafting?					
Roller's judgment	100.0	100.0	100.0	100.0	100
Roller's pass practice recorded?					
No	80.0	60.0	69.7	60.0	69
Metallurgical Observer	20.0	40.0	30.3	40.0	31
Pass and Draft Recorder	0.0	0.0	0.0	0.0	0
Tandem Rolling					
Not used	80.0	57.2	82.6	100.0	84
Used except on special grades	20.0	42.8	17.4	0.0	
Rolling Top First	40.0	85.7	63.1	43.75	58
Rolling Butt First	60.0	14.3	36.9	56.25	
Minimum Edge Reduction, Ingot-Product					
None	0.0	0.0	2.3	100.0	22
1-3 in.	20.0	14.3	2.3	0.0	5
3-4 in.	60.0	71.4	55.8	0.0	47
5-6 in.	20.0	0.0	37.3	0.0	23
7-8 in.	0.0	14.3	2.3	0.0	3
Maximum Edge Reduction, Ingot-Product					
None	20.0	57.1	77.3	93.8	71
3-6 in.	20.0	0.0	0.0	6.2	4
7-10 in.	20.0	0.0	0.0	0.0	4
11-14 in.	20.0	28.6	18.2	0.0	15
15-18 in.	20.0	14.3	4.5	0.0	6
Regulation of Roll Water					
None	20.0	14.3	15.2	31.3	19
Sprays	60.0	42.8	69.6	62.5	63
Water Box	10.0	28.6	13.0	6.2	13
Wipers	10.0	14.3	2.2	0.0	5

Av. Finishing Temperature and Ranges °F	Av.	Ranges	Av.	Range	Av.	Range	Av.	Range
Under .10C Rimmed	2045	1930-2100	2055	2000-2100	2070	1900-2100	2095	2000-2155
Under .10C Killed	2055	2000-2100	2065	2000-2100	2085	2000-2125	2100	2100 —
.15-.30C Semi-killed	2025	1925-2100	2050	2000-2100	2010	1800-2125	2005	1900-2100
C1018-1022 Hot Tops	2015	1975-2150	—	—	2055	2000-2125	2090	2000-2120
C1045 Hot Tops	2040	2000-2075	—	—	2030	1850-2100	2100	2000-2175
C1113-C1213 Resulfurized	2075	2050-2100	—	—	2090	2000-2150	2045	1900-2130

Table II. Group A—Ten Mills: Universal Slabbing Mills

Mill Statistics		Range	Average
Nominal mill size, in.		36-45	43.4
Max lift bullhead, in.		32-80	52.5
Horizontal roll body, in.	Length	70-120	90.0
	Diam	36-46	42.6
Vertical roll body, in.	Length	34-96	60.0
	Diam	26-40	31.0
Drive horsepower	Horizontal	7000-12000	9500
	Vertical	1650-4000	2670
Open Top and Bottle Top Ingots			
No. sizes per mill		5-16	8.7
Thickness, in.		20-35	27
Width, in.		30-87	49.5
Cross section, in.		24 x 34 to 33 x 72	27 x 49.5
Area of cross section, sq in.		816-2376	1336
Hot Top Ingots			
No. of sizes per mill		0-11	
Thickness, in.		16-39	28.3
Width, in.		28-72	51.0
Cross section, in.		25 x 28 to 39 x 72	28 x 51
Area of cross section, sq in.		700-2808	1443
Slab Sizes Rolled			
Slab thickness, in.	Min	2-4	3.5
	Max	5.5-22	11.5
Slab width, in.	Min	18-28	20.0
	Max	58-78	67.0
Rolling Practice			
Passes per ingot	Edge	0-8	2.0
	Flat	6-23	10.9
	Edge & flat combination	1-28	5.0
	Total	10-33	17.9
Draft per pass, in.	Edge	$\frac{1}{8}$-4	1.6
	Flat	$\frac{3}{8}$-3	1.5
Reduction			
Reduction—Ingot to slab, in.	Edge	2-16	5.9
	Flat	16-28.5	22.0
Reduction in area ingot to slab, sq in.	Total	600-2164	1154
Pct of original ingot cross section		68-88.5	81.0
Reduction in area	Per pass	21.4-116	66.5

Top or Butt First Rolling: Forty-two pct of the respondents roll ingots butt first as normal practice. A considerable number of mills indicate a preference for butt first rolling but are unable to do so because no ingot turn around is available. Butt first practice is favored, since maximum yield is obtained and pipe minimized. Reportedly, it also facilitates shearing and reduces split ends.

Ingot to Product Reduction Limits: No limit on ingot to product edge reduction is set by the Group D mills. Most mills do not set a maximum reduction limit, and those which do have limits of the order 11 to 18 in. Except for Group D, most mills have a minimum limits of 3 to 6 in. edge reduction from ingot to product.

Roll Cooling Water: Only 20 pct of the reporting mills do not control roll cooling water. Where roll water is controlled, 60 pct use sprays and about 20 pct use wipers and water boxes. A number of mills report that water is completely shut off while rolling certain grades, such as high sulfur or low manganese steels.

Initial Drafts: Drafts used on initial passes are usually substantial and of the order of about 2 in. Usually no deference to a possibly weak as-cast ingot structure is indicated. Somewhat lighter drafts and more frequent turns are used in bloom rolling in the Group D mills.

Finishing Temperatures: Rolling temperature ranges and averages are given in Table I. The desired finishing temperatures vary widely, and there is a distinct trend to higher temperatures at the Group D mills.

Universal Slabbing Mills

Mills and Rolls: Universal slabbing mills, designed especially for slab rolling, are capable of very high rolling rates. Ingots larger in width than tolerable on blooming mills may be rapidly reduced by quick sequences of concurrent slabbing and edging work. Strong, heavily powered horizontal rolls without edging grooves render fast effective passes possible.

Table III. Group B—Seven Mills: High-Lift Blooming Mills Rolling Slabs Only

Mill Statistics	Range	Average
Nominal mill size, in.	40-54	45.1
Max lift, bullhead, in.	52-68	60.6
Horizontal roll body		
Overall length, in.	90$\frac{1}{2}$-115	102.6
Bullhead length, in.	59-79	66.7
Bullhead diam, in.	37$\frac{1}{4}$-46	42.6
Collar diam, in.	37$\frac{1}{4}$-49	43.4
Edging passes	2-3	2.4 per mill
Pass widths, in.	12-14$\frac{1}{2}$	13-25
	7$\frac{1}{2}$-9	8.1
	5-7	5.9
Open Top and Bottle Top Ingots		
No. sizes per mill	2-7	4.4
Thickness, in.	20-33	26.0
Width, in.	24-65	46.0
Area, sq in.	480-1820	1205.4
Cross-section, in.	20 x 24 — 28 x 65	26.0 x 46.0
Hot Top Ingots		
No. sizes per mill	0-3	1.3
Thickness, in.	23-29	26.1
Width, in.	42-54	43.9
Cross section, in.	26 x 26 — 26 x 54	26.0 x 43.9
Area of cross section, in.	674-1404	1139.4
Slab Sizes Rolled		
Thickness, in.	2$\frac{1}{4}$-10	3.3-7.6
Width, in.	10-63$\frac{1}{2}$	19.6-55.1
Typical Rolling Practice		
Ingot size		
Width x thickness, in.	21 x 34 — 28 x 68	26.0 x 48.6
Area, sq in.	714-1904	1272
Slab size		
Width x thickness, in.	29 x 5 to 63 x 5$\frac{1}{4}$	41.2 x 5.1
Area, sq in.	132-392	293
Reduction of area, ingot to slab		
Total, sq in.	500-1442	1056
Per pass, sq in.	33-80	57
Number of passes per ingot	15-27	18.5
Draft per pass		
Edge, in.	$\frac{1}{8}$-6	2.0
Flat, in.	$\frac{3}{8}$-3$\frac{1}{2}$	1.6
Initial passes		
Edge (in sequence)	0-4	2.0
Draft, in.	$\frac{1}{2}$-4	2.3
Flat (in sequence)	0-6	1.1
Draft, in.	1$\frac{1}{2}$-2	1.7
Max draft used		
Edge, in.	1$\frac{1}{4}$-6	3.0
Flat, in.	1$\frac{1}{2}$-3$\frac{1}{2}$	2.3
Min draft used		
Edge, in.	1-2$\frac{1}{4}$	1.9
Flat, in.	$\frac{3}{4}$-2$\frac{1}{2}$	1.5
Edging passes in sequence	1-4	2.1
Draft per pass, in.	1$\frac{1}{4}$-4	2.3
Slabbing passes in sequence	6-16	8.3
Draft per pass, in.	1-4	1.7

The reduction rates actually practiced are, of course, regulated to suit various metallurgical and operating circumstances. The time consuming extreme screw lifts required for edging on high-lift blooming mills are largely avoided on slabbing mills.

Six companies operating ten universal slabbing mills replied to the questionnaire. All are capable of rolling slabs to at least 58 in. wide; four are high lift mills with 66 to 80 in. maximum horizontal roll opening, and six are low-lift mills with maximum roll opening of 32 to 48 in. Mill size is usually about 45 in., but two of the older mills are 36 and 40 in. in size. One of the ten mills is steam driven, but the trend is toward twin electric drive motors of about 12,000 hp on the horizontal rolls in the newer mills. Summary of mill and roll size information is made in Table II.

Ingot Sizes: All mills, with one exception, are provided with ingot sizes large enough to produce the widest slab the mill is capable of rolling. Unusual practice at one mill is to cross roll the ingot to achieve a slab width 17 in. wider than the ingot. The number and range of ingot sizes provided is much the same for both high-lift and low-lift mills, except that the use of extremely wide ingots—80 in. or more—is confined to the high-lift mills which are presumably better equipped to edge down the wider ingot. Summary of ingot dimensions is set out in Table II.

Two cases are noted where open top ingot thickness is standardized. At one plant no ingots exceed 22 in. in thickness and at another none are less than 30 in. In all other cases, ingot thickness ranges from 22 in. at least through 26 in. and usually to 30 in. or more.

Slab Sizes: Table II gives a summary of slab sizes rolled on the slabbing mills. The range of slab sizes given in the typical reports runs from 28 x 4 in. to 75 x 4 in. and 66 x 10½ in.

Drafting Practice: There is a substantial variance in drafting procedure from mill to mill. Differences arise chiefly from limitations of the physical equipment of mills, the slab sizes required, and compromises made to achieve most generally effective production. In general, low-lift mills use about one more pass per ingot than high lift. Drives of 10,000 hp or more use an average of about one less pass per ingot than the less powerful drives. A summary of rolling practice is necessarily limited to broad analysis of pertinent features.

A review of the typical rolling procedures reported indicates that substantial initial drafts, 1½ in. or more, are the rule. Several initial passes in sequence without turning are common. After breakdown passes, there are frequently seven or more heavy slabbing passes with no edge reduction indicated. In this group of mills, however, it would be expected that edge work equal to spread is accomplished with each flat pass. Light sizing drafts of 1 in. or less are the rule for the final two or three passes. There is no substantial distinction in rolling practice for the 0.10 carbon steels, rimmed or killed, semi-killed 0.15-0.30 carbon, or the C1018, 1022, or 1045 grades. Pass practice seems regulated more to suit reduction required rather than the steel chemistry.

Table II summarizes typical ingot to slab reduction, as carried on in the 10 universal slabbing mills. The total number of passes, as well as use of edge,

Table IV. Group C—46 Mills Producing Both Slabs and Blooms

Mill Statistics		Range		Average
Nominal mill size, in.		32-54		42.7
Max lift, bullhead, in.		22-68		44.3
Horizontal roll body				
Bullhead diam, in.		24-46.5		37.2
Length (collar to collar), in.		67-140		95.6
Width of flat working face, in.		26-96		48.5
Horsepower				
Electric-driven		3,000-12,000		6,870
Steam-driven		8,000-25,000		15,125
Open Top and Bottle Top Ingot				
No. of sizes per mill		1-15		5.8
Thickness, in.		16-36		25.2
Width, in.		18-66		39.1
Cross section size, in.		16 x 18	to 25.2 x 31 x 56	39.1
Area of cross section, sq in.		288-1944		985
Hot Top Ingot				
No. sizes per mill		1-10		3
Thickness, in.		13-42		25.7*
Width, in.		18-72		35.3*
Cross section size, in.		13 x 16 to 46 x 57		25.7 x 35.3
Area of cross section, sq in.		208-2622		907
Slab Sizes, Rolled				
Slab thickness, in.	Min	1.5-5		3.4
	Max	4-24		8.5
Slab width, in.	Min	9-34		19.5
	Max	15.2-63		34.9
Bloom Sizes Rolled				
Bloom thickness, in.	Min	3.7-10		6.2
	Max	4.5-24.5		11.8
Bloom width, in.	Min	4-12		6.9
	Max	8-24.5		14.7

* These values do not include data for several sizes of round molds ranging from 24 (461 sq in.) to 30 (720 sq in.) in diam.

Table V.

	Rimmed Steel Under 0.10 Pct Carbon	
	Range	Avg
Pct mills reporting this grade		76
Pct mills rolling this grade to:		
Slabs		100
Blooms		16
Ingot Size, in.		
Width	23.0-66.0	40.2
Thickness	21.0-31.0	24.3
Slab or Bloom Size, in.		
Width	12-58	32.4
Thickness	2.8-8.5	5.0
Total Reductions, in.		
Edge	3-16.2	9.7
Face	13.2-28.7	19.6
Total Reduction in Area, pct	72-94.8	83.5
Number Passes Used	11-25	16.8
Initial Passes in Sequence		
Edging,	1-4	2.1[a]
Followed by Slabbing Passes	2-10	7.2[a]
Slabbing Passes,	2-10	5.0
Followed by Edging Passes	1-4	3.0
Max No. of Edging Passes in Series	2-8	2.6
Max No. of Slabbing Passes in Series	4-12	7.2
Max Drafts Used		
Edging	1.5-4.0	2.2
Slabbing	1.0-3.0	2.1
Min Drafts Used		
Edging	0.2-2.0	0.9
Slabbing	0.2-2.0	1.0
Finishing Temp, °F	1800/2100	2070

Typical Rolling Practices with Six Grades of Steel at 46 Group C Mills

Killed Steel Under 0.10 Pct Carbon		Semikilled Steel SeFor Plates 0.15-0.30 Pct C		Hot Tops C1018-C1022		Hot Tops C1045		Open Top Resulfurized C1113-C1213	
Range	Avg	Range	Avg	Range	Avg	Range	Avg	Range	Avg
	59		54		72		54		50
	95		100		0		4		0
	10		12.5		100		96		100
23.0-66.0	45.8	24.0-66.0	44.2	22.0-35.0	27.1	20.0-42.0	26.6	22.0-34.0	27.1
22.0-32.0	26.1	21.0-32.0	26.6	19.0-32.8	25.2	20.0-32.8	25.1	19.0-26.0	24.7
16.0-59.0	38.8	17.0-60.0	36.3	4.0-15.5	10.2	4.4-14.0	9.8	4.0-13.8	9.1
3.5-12.0	6.1	3.5-9.5	4.6	4.0-14.0	8.6	4.4-13.8	8.3	4.0-13.8	8.2
3.7-13.0	9.5	3.7-12.5	8.8	8.5-23.6	17.8	11.5-29.3	17.8	15.0-26.6	19.1
14.0-27.5	20.4	16.8-37.5	27.8	10.0-23.1	17.3	10.0-29.2	17.4	13.0-24.5	17.7
58.0-89.5	81.4	72.7-92.2	85.3	69.7-97.2	86.5	76.0-96.0	87.5	76.0-96.6	89.2
11-25	18.4	15-25	19.7	11-28	17.7	12-31	18.3	13-31	19.9
2-4	2.3	1-4	2.1[a]	2-6	2.5[a]	2-6	2.7[a]	2-4	2.8[a]
4-12	7.4	2-11	7.1[a]	2-6	3.9[a]	2-6	4.0[a]	2-6	4.2[a]
—	—	2-14	8.0	2-10	3.1	2-10	3.3	2-8	2.7
—	—	1-4	2.1	2-8	2.9	2-4	2.6	2-4	2.2
2-4	2.4	2-4	2.3	2-6	3.7	2-6	3.8	2-6	4.0
4-12	7.6	4-12	8.1	4-10	5.0	2-10	4.9	3-8	5.0
1.0-3.2	2.7	1.0-3.0	2.2	2.0-5.0	2.7	2.0-4.0	2.9	2.0-4.0	2.8
1.5-3.0	2.4	1.0-3.0	2.1	2.0-5.0	2.8	2.0-4.0	2.6	2.0-8.0	2.5
0.2-2.0	0.9	0.2-2.0	0.8	0.2-2.5	1.2	0.2-2.0	1.3	0.2-2.0	1.2
0.2-1.5	1.1	0.5-1.5	0.8	0.2-2.2	1.0	0.2-2.5	1.0	0.2-2.0	0.9
2000/2125	2055	1800/2125	2010	2000/2125	2055	1850/2100	2030	2000/2150	2092

flat, and combination edge-flat passes, varies widely. Total passes range from 10 to 33 per ingot with the average being 18. Initial or breakdown passes are usually no more than one or two in sequence, either edging or flat. Slabbing passes in sequence without edging reduction are indicated to be 8 or 9 per ingot at about 1½ in. draft each.

Thirteen examples show one or two 180° turns per ingot; thirty-five examples show one to four 90° turns; and six examples indicated no turns.

Draft and Reduction: Total edging reduction cited in the examples averages about 6 in., while total slabbing reduction averages 22 in. per ingot. Draft per pass, edging, averages 1½ in., and draft per pass, slabbing, averages 1½ in. Slabbing draft is usually less than 1¾ in. per pass.

Summary of Universal Slabbing Mills: The mills in this group are generally of the order of 45 in. pitch size; they have powerful drives with the newer mills carrying 12,000 hp on the horizontal rolls and 4,000 hp on the vertical. Ingots to 87 in. wide are rolled into slabs to 78 in. wide. Edge reduction averages 6 in. and flat reduction 22 in., accomplished in an average of 18 passes with edging and slabbing drafts of the order of 1½ in.

High-Lift Blooming Mills Rolling Slabs Only

General: Replies from high-lift blooming mills rolling slabs only were received from seven mills in seven separate companies. All are electrically powered, five at 6,000 to 7,000 hp and two at 10,000 hp. Two newer mills have twin 5,000 hp drives.

Five of the seven mills specialize in slab rolling to best production advantage, since another mill is available in the same plant for miscellaneous bloom and slab rolling. The best suited blooming mill may then be provided with optimum ingot and slab sizes so as to obtain maximum output. Two mills are the only primary reduction mills in their respective plants which produce flat-rolled products only. The general mill statistics are shown in Table III.

Ingot Data: Some difference of opinion is indicated as to the importance of ingot thickness as an influence on segregation; range was 20 to 33 in. with an average of 26 in. Ingot width used by the Group B mills is from 24 to 65 in. As in the case of ingot sizes supplied to the universal slabbing mills, size appears to be more a function of providing requirements than metallurgical restrictions. Statistics on ingot size are found on Table III.

Slab Sizes: Pass practice data showed no cases where cross rolling of ingots was used to obtain the desired slab width. The range of slab sizes required from the Group B mills was greater than that from the Group A mills.

Typical Rolling Practice: A tabulation of the typical rolling practice of the seven mills of Group B is shown in Table III. The reduction of area, ingot to slab, ranges from 500 to 1,440 sq in. with an average of 1,056 sq in., somewhat lower than the universal slabbing mills. The reduction of area per pass is lower and the number of passes used is higher in the case of the high-lift blooming mills than with the slabbing mills.

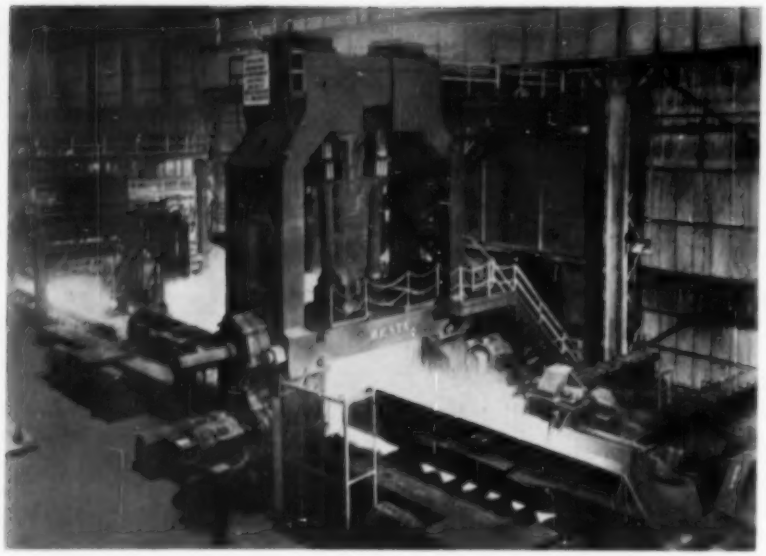
The initial edge or flat passes used to break down the primary *as-cast* ingot structure are not necessarily light passes. Rolling practice is to draft heavier on the initial passes than in the succeeding passes. Effective initial passes seem to be the rule.

The maximum edge draft used ranges from 1¼ to 6 in. while the maximum *flat* draft ranges from 1½ to 3½ in. In each case the heavy drafts are associated with the more powerful mills. Drafting practice appears to be largely regulated by drive horsepower. Half of the reporting mills indicate that drive horsepower affects their drafting practice.

Mills Rolling Both Slabs and Blooms

Of blooming mills currently in use, the largest division is those which roll both slabs and blooms. About 60 pct of the mills fall into this category.

Physical Dimensions: A summary of the mill dimensions for this group is shown in Table IV. The average mill pitch size is 43 in. and ranges from 32 to 54 in. Seventy-five pct of the mills are classed as 40, 44, or 46 in. sizes. The roll diameter ranges from 24 to 46½ in. with the majority close to the average of 37 in. Twenty-six different roll lengths are in use



The latest in slabbing mill design includes the 45-in. x 90-in. universal reversing slabbing mill built by Mesta Machine Co. This mill is capable of high rates of output to supply the widest of continuous strip mills. At left is the 2.5 million ton capacity mill at the Fairless Works of U. S. Steel Corp. Below is the same type mill operating at the Encorse, Mich. plant of Great Lakes Steel Corp.

between 67 to 140 in. One-fourth of the mills in this group are powered with steam engines, with the most predominant rating being 7,000 hp. Although 60 pct of the mills use three edging grooves, there is equal preference among the remainder for two, four, or five grooves. The large groove averages 13 in. and ranges from 9 to 20 in., while the second and third passes average 8 and 6 in., respectively. The typical blooming mill rolling both slabs and blooms is a 44 x 96 in. mill with maximum roll opening of 44 in. Roll passes are 48 in. bullhead, 13, 8 and 6 in. edgers.

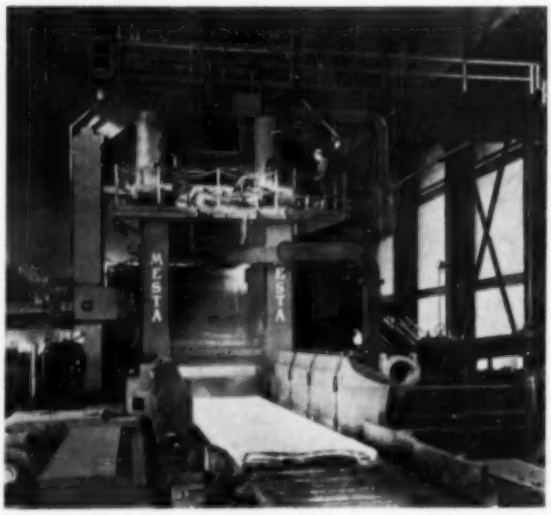
Ingot and Product Size: Table IV shows that an average of six open top and three hot top sizes are provided per mill. Of some 100 different open and bottle top ingot sizes, 75 pct are 26 in. thick or less. For hot top ingots this condition is reversed, with the majority exceeding 26 in. in thickness.

Slab and Bloom Sizes: Slab widths as shown in Table IV, extend from 9 to 63 in. and in thickness from 1½ to 24 in., but the usual range is 19 to 35 in. in width and 3½ to 8½ in. in thickness. The average bloom sections which may be rolled are from 6 x 6 in. to 12 x 15 in. Breakdown sizes are excluded.

Rolling Practice: A tabulation of the typical rolling practices is shown in Table V. Examples from over one-half of the 46 mills are summarized for each of the steel grades. The average total flat and edge reduction for bloom sections is 17½ in. and ranges from 8 to 29 in. For the slabbing grades, edge reduction averages 9½ in. Total flat reduction for the low carbon rimmed and killed grades is about 21 in. The reduction in area for the blooms averages 87 pct, while slab sections ranged from 81 to 85 pct. The average number of passes used per ingot, 18 for blooms and 19 for slabs, are similar to those mills rolling these products individually.

The usual initial pass sequences for slabs consist of two edging passes followed by an average of seven flat passes. For blooms, this sequence is three edging and four flat passes.

The average maximum edging draft for blooms is 3 in. and ranges from 2 to 5 in. For slab ingots these values are 1 to 4 in. The maximum flat draft used on bloom sections, 2½ in., is also slightly higher compared with the practice for slab ingots. The minimum draft used is approximately 1 in. and ranges from ⅛ to 2 in.



Blooming Mills Rolling Blooms Only

General and Roll Data: Sixteen of the total responding mills are blooming mills rolling blooms only. Of this group five are steam-driven and the balance are electric. Three of the 16 mills have fixed passes, i.e. fixed top rolls and no roll lift. The balance have an average lift of 35½ in. ,with a range of 24 to 48 in. In this group mill horsepower varies from 2800 to 15,000 for steam drives and 1000 to 8000 for those powered electrically.

Three to four edging grooves are the rule in this group with a range of two to five. Edging pass sizes are of the order of 5½, 8 and 14½ in. respectively. The wide edging groove varies from 10 to 20 in. A typical mill rolling blooms only might be described as a 40 x 82 in. mill with three edging grooves—14½, 8 and 5½ in. Bullhead width averages about 30 in.

Ingot Data: A range of one to seven open top ingot sizes are provided for the mills of this group, and the average ingot size is 23 x 26 in.

The number of hot top sizes per mill ranges from 0 to 4, and the average hot top ingot size is 25 x 27 in. The hot top ingots are dimensionally bigger than

Table VI. Group D—16 Mills: Blooming Mills Rolling Blooms Only

Mill Statistics	Range	Avg
Nominal mill size, in.	32-46	38.5
Max lift, bullhead, in.	23-48	35.4
Horizontal roll body		
Overall length, in.	55-100	81.6
Bullhead length, in.	21-39	29.9
Bullhead diameter, in.	28-42	32.2
Collar diameter, in.	23-46	36.3
Edging passes	2-5	3.4
Pass widths, in.	10-50	14.4
	7-9	8.2
	4-7	5.5
Open Top & Bottle Top Ingots		
No. sizes per mill	1-7	2.5
Thickness, in.	16-32½	23.2
Width, in.	16-40	25.8
Cross section, in.	16x16-28x40	23.2 x 25.8
Area of cross section, in.	324-1120	615.3
Hot Top Ingots		
No. sizes per mill	0-4	1.25
Thickness, in.	19-34	25.0
Width, in.	22-34	27.1
Cross section, in.	19x22-34x34	25.0 x 27.1
Area of cross section, in.	483-1156	691.3
Bloom Sizes Rolled		
Thickness, in.	4-24	8.7
Width, in.	4-24	9.5
Typical Rolling Practice		
Ingot Size		
Thickness x width, in.	17x22-32½x32½	24.0 x 25.9
Area, sq in.	374-1056	649.1
Bloom size		
Width x thickness, in.	4x4-15x15	9.1 x 8.1
Area, sq in.	16-225	79.5
Reduction of area, ingot to bloom		
Total, sq in.	358-991	573.0
Per pass, sq in.	18-48	28.6
Number of passes per ingot	11-29	20.4
Draft per pass, in.	⅛-3	1-7
Initial passes	0-4	2.2
Draft per pass, in.	1-2½	1.9
Max draft used, in.	1½-3	2.4
Min draft used, in.	⅛-1¾	0.7
Max passes in sequence	4-7	5.3
Draft per pass, in.	1½-2½	1.9

the open top ingots in this group while the reverse is true in the other groups studied.

Bloom Sizes: The bloom sizes rolled by this group of mills range from 4 to 24 in. both in thickness and in width. The average bloom size is 8½ x 9½.

Typical Rolling Practice: A tabulation of the typical rolling practice of the group (excluding the fixed pass mills) is given in Table VI. From the data it will be seen that the average reduction is only 573 sq in. and the reduction per pass averages 28.6 sq in. This is the lowest rate of reduction of all four groups. The average number of passes per ingot, 20, is the highest of the four groups reviewed. The average draft on the initial passes is slightly higher than that taken on the succeeding working passes.

Maximum passes in sequence range from four to seven, with an average of 5¼. Draft ranges from 1½ to 2½ in. with an average of 2 in. The minimum number of passes in sequence is two with an average draft of 1⅝ in. ranging from ¼ to 2½ in.

The typical drafting and pass practice examples show more tendency toward light drafts and frequent turns than noted on groups A, B, and C. The major fraction of the rolling practice examples are in the C1045, C1018-1022 and C1113-C1213 categories where surface requirements are perhaps critical. Eight of the 16 mills in the group indicated that mill motor horsepower is a limiting factor in determining the pass and draft practices.

Summary

It is recognized that rolling practices frequently deviate from the ideal because of production requirements and limitations of equipment. There is probably no instance where practices might not be revised if circumstances permitted. The intent of this review has been to present facilities and procedures in a resumé which might serve as a standard of comparison.

The Sendzimir Cold Strip Mill

by Michael G. Sendzimir

IN the early nineteenth century, when the production of flat ferrous products passed from the forging hammer into the rolling mill, the first type of mill used was one consisting of a single pair of cylindrical rolls. This is called a 2-high mill. Its reducing capacity depends on the rigidity of the rolls to withstand the bending effect, and on the diameter of the roll to reduce the contact area in the roll bite. These two requirements are juxtaposed to each other, and for that reason, it is necessary to keep the barrel length:diameter ratio at the maximum of 2 or even 1.5. Therefore, the wider the mill, the larger is the roll diameter, and soon the practical limit of the width is reached.

Another important limit, reached by increasing roll diameter, is the roll flattening effect which occurs when material is reduced to thin gages. It prohibits further reduction of the material being rolled, because with the increase in pressure between the two rolls, the arc of contact between the rolls and the metal increases out of proportion, and the specific pressure needed to reduce the metal decreases.

Three-High Mill, an Improvement

With the 3-high mill, a third roll of smaller diameter is introduced between the rolls of the 2-high mill. In this way the mill is made more rigid against deflection, while the roll flattening effect is less; thus, metal can be further reduced under the same set of conditions.

The 3-high mill had its day of glory when rolling was confined to sheets or plates. It was particularly well adapted for this purpose, since with a single direction drive the sheet could be passed in either direction through the mill simply by entering at the bottom or the top of the work roll.

Four-High Mill a Natural Outgrowth of the Three-High Mill

The 4-high mill, adopted by the steel industry for cold rolling sheet and strip in the years following World War I, makes use of two small diameter work rolls with two backup rolls. The mill benefited from many modern metallurgical and mechanical developments, but its fundamental weakness was its support, which acted as a simple beam, deflecting under heavy rolling loads. In order to make the mill more rigid, the backup roll approached a width:diameter ratio of nearly unity. With increasing width, the 4-high mill, thus, became of tremendous size. Yet, even though heavier passes could be taken and thinner gages could be reached with harder metals, the roll flattening effect still caught up with the mill, and there was a limitation on the minimum gage that could be rolled.

MICHAEL G. SENDZIMIR is Treasurer of the Armzen Co., Waterbury, Conn. This paper was presented at the AIME New England Regional Conference, Cheshire, Conn., April 20 and 21, 1956.

Cluster Mill Developed in Search for Smaller Diameter Work Rolls

The first Sendzimir mills were put into operation in Europe in 1932. While this mill is a type of cluster mill, there are certain important differences between it and other cluster mills. The fundamental idea of the Sendzimir mill is the backing arrangement, characterized by one single compact and rigid housing, which holds backing rolls and work rolls in place. The roll seperating force in the Sendzimir mill passes from the work rolls through the intermediate rolls to the backing shafts. These shafts have concentrically mounted bearings of the roller type which are located eccentrically in the saddle rings, equally spaced between the bearings. These transmit the work roll separating force directly to the rigid housing across every inch of mill width.

There are two fundamental reasons that brought about the development of the Sendzimir mill. First, with the increasing size of the 4-high mill, mill costs began to rise astronomically; the Sendzimir mill could be built for a great deal less. Second, the small work rolls of the cluster mill promised to roll strip to a much closer gage accuracy than possible on other mills.

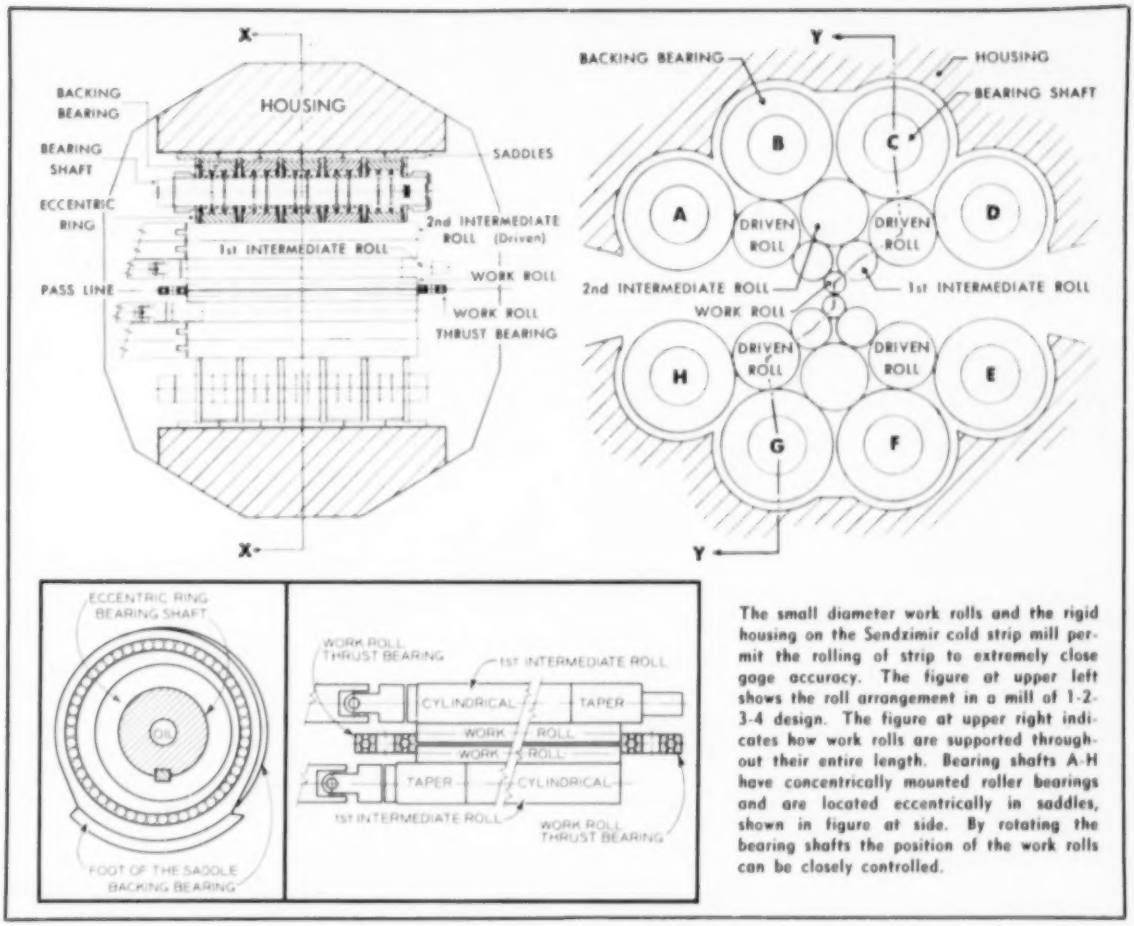
Evolution of Sendzimir Mill Continues

The original Sendzimir mill, which is still operating in Poland, had two 4-in. diam work rolls, each of which was supported by two backing shafts which rotated eccentrically in the saddle rings and opened or closed the mill.

1-2-4 Mill: On subsequent mills it was necessary to give a heavier backing capacity to make heavier reductions on the strip. The use of a smaller diameter work roll was made possible by adding another backing stage. Consequently, the backing shafts were replaced by solid rolls, and each of these rolls was in turn backed up by two back-up shafts. In this arrangement the work rolls were driven, one from each side of the mill. Their diameter was about 3 in., and the backing bearings were 9-in. diam. This 1-2-4 combination proved to be a long step forward, and today two 50-in. side mills of this type are still in commercial operation.

It was recognized at that time that with the reduction in the work roll diameter, the tension on the strip should be somewhat heavier than for comparable reductions with larger diameter work rolls. Furthermore, this tension had to be much more constant and, therefore, necessitated a carefully designed new tension apparatus.

Tension control was a remarkable feature of the early Sendzimir mills. It consisted chiefly of two



The small diameter work rolls and the rigid housing on the Sendzimir cold strip mill permit the rolling of strip to extremely close gage accuracy. The figure at upper left shows the roll arrangement in a mill of 1-2-3-4 design. The figure at upper right indicates how work rolls are supported throughout their entire length. Bearing shafts A-H have concentrically mounted roller bearings and are located eccentrically in saddles, shown in figure at side. By rotating the bearing shafts the position of the work rolls can be closely controlled.

large drums on each side of the mill housing, on the outside of which were attached smaller rolls in such a fashion as to form a number of little pinch rolls. This system could apply the necessary front and back tension to the strip. The mill was set up so that a constant elongation was always given to the strip under this tension arrangement and drive, and the roll bite was adjusted accordingly. Whenever a softer spot occurred in the strip, the elongation would remain the same but the specific tension would lessen, and the strip would be reduced less. Conversely, when a harder portion of the strip came to the roll bite, the specific tension would then be heavier, and the strip would be reduced more.

During the war years, the Sendzimir mill acquired the modern tension control of a full reversing mill which has constantly been improved and whose uniformity today permits the Sendzimir mill to roll strip of extremely close gage tolerance in the lengthwise direction.

Most small Sendzimir mills have been equipped with *operating tensiometers* which control the tension of the winder reels. The larger mills have been equipped with current regulators, and at present some new mills will have *indicating tensiometers*. The latest development is the use of the mercury arc rectifier, which is gaining popularity on the Continent and may become a strong competitor to motor-generator set drives.

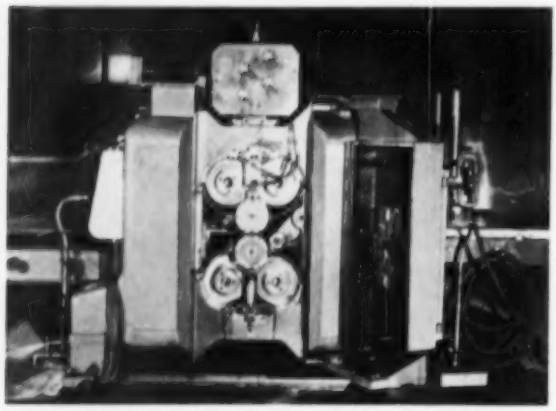
Postwar Mills: During World War II and immediately afterwards, the Sendzimir mill underwent a considerable change. First came a 1-2-3 arrangement in which the work rolls were supported by two intermediate rolls that were driven, and these two driven rolls were in turn supported by a total of three backing-up shafts on the upper and on the lower side of the mill. This arrangement permitted the use of tungsten carbide work rolls and increased the power that the mill could transmit.

1-2-3-4 Mill: In order to get the smallest possible diameter work roll and, at the same time, to give the maximum possible reduction to the strip, a 1-2-3-4 mill was developed in recent years.

In the 1-2-3-4 Sendzimir mill, shown above, there are eight backing shafts, numbered *A*—*H* in the clockwise direction, starting at 10:00 o'clock. Shafts *B* and *C* are the main screwdown shafts which are equipped with large hydraulic cylinders on the top of the mill. These shafts have roller bearings in the saddle rings and can be easily rotated under the heavy screwdown pressure. All other shafts have plain bearings in the saddle rings and can be rotated only under no-load condition. The other shafts are also self-locking, i.e., in order to open or close the mill the shafts have to be positively moved. Shafts *A* and *H* are moved by an electric motor located in the back of the mill, and shafts *D* and *E* are moved by a similar motor. These shafts are brought closer together or further apart, depending on the size of the rolls in the mill.

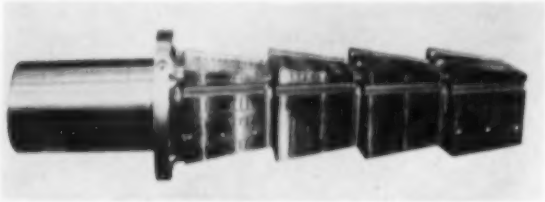
Shafts *F* and *G*, the two bottom shafts, are moved by a hydraulic cylinder located in the front of the

mill. These shafts are opened or closed in order to change the work rolls in the mill. The movement of these shafts serves two purposes. First, it brings the work rolls to the pass line of the mill and, therefore, provides an even bearing of the work roll and surfaces against the thrust bearings in the front and back door of the mill. Secondly, and this is more important, the closing of the bottom rolls takes out all the slack between the rolls and enables the full travel of the top screwdown of the mill. This permits the operator to reduce thick strip of hot rolled gage down to thinnest gages without changing the work rolls in the mill.



This ZS 06-50 Sendzimir mill of design similar to the original Sendzimir mill is well adapted for rolling stainless steel sheet. Mill shown is at the Welland, Ontario plant of Atlas Steels Ltd.

Shaft *D* is equipped with a crown control. On the right side of the mill are located bolts corresponding to each saddle on shaft *D*. When there is no-load in the mill, these bolts can be rotated, and each individual saddle on shaft *D* can change its height with respect to the housing; in other words this shaft has doubly eccentricity. This adjustment enables the operator to give any shape to the mill he desires by adjusting these bolts.



Because of the heavier tensions used on the Sendzimir mill, it was necessary to develop a new winder design. On this quadruple pyramid winder, the inner shaft pushes the outer elements forward and brings them to collapse.

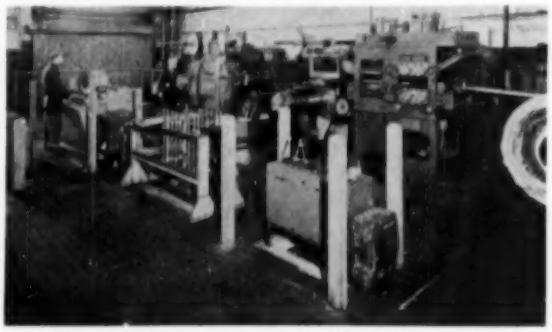
On the 80 in. wide Sendzimir mill, similar adjustment is provided on shafts *B* and *C*, acting simultaneously on the two shafts. That adjustment can be made under load and, therefore, the operator can change the shape of the strip while the mill is rolling. Some recent applications of the Sendzimir mill will utilize this principle of *as-u-roll* crown adjustment, which is motorized through small hydraulic motors and can be controlled by the operator from the operating pulpit.

The work roll diameters of the different size 1-2-3-4 mills are as follows: 2⅛ in., 1½ in., 1⅛ in., ⅞ in., ½ in., and ¼ in. The backing bearing diameters range from 1½ in. to 12 in.

The roll separating force on the 1-2-3-4 Sendzimir mill is distributed from the work rolls to the intermediate rolls and then to the backing up shafts in such a fashion that the outer shaft takes a heavier force component, i.e., absorb more load than the center shafts. The driven rolls on the mill are the outer second intermediate rolls. The choice of driving these rolls enables the designers to incorporate larger size pinions and, therefore, the mill can transmit more torque.

The size of the pinion stand is made bearing in mind the width of the mill, the type of metal to be rolled, and the production that the mill is supposed to attain. Strictly speaking, it has to be commensurate with the amount of torque that is supposed to be transmitted to the work rolls.

The first intermediate rolls on the Sendzimir mill can be axially adjustable from the front of the mill by two handles which, through a mechanical coupling, shift the rolls to the front or the back of the mill. This feature is very important for the operation of the mill, since these rolls are ground with a taper. The top rolls have a taper from the front side and the bottom rolls from the rear side. In this way, with an independent movement of the top and the bottom rolls, it is possible to control the shape of the edges of the strip to an extremely fine degree. The conventional rolling mill is designed for a constant pressure rolling, and each pass is made at a given pressure between the rolls. For that reason the reduction in the mill during each pass, i.e., the percentual reduction, will greatly differ. There are no such limitations on the Sendzimir mills.



At Sharon Steel Corp., Sharon, Pa. this ZR 23-25 Sendzimir mill of the 1-2-3-4 design is in operation. Note the rack with roll changes in left foreground.

Lubrication is made by mineral oil for stainless, and non-ferrous metals, as well as some low carbon installations, while some Sendzimir mills are lubricated by soluble oil. The lubricant enters from the back of the mill through an annulus around the back door and is then distributed to the centers of each of the backing shafts that are drilled axially for this purpose. The lubricant passes through these shafts, then flows radially through the bearings, and through holes that are drilled in the backing shaft in the radial direction. In this way the bearings are lubricated and cooled. The oil next flows over the rolls and finally onto the strip and extracts some of the heat from it. The oil finally escapes through two pipes located in the front and back of the mill.

Additional lubrication is provided to the strip and the work roll bite proper in order to make the reductions of the steel easier. The oil flows through

headers located immediately adjacent to the work roll bite where it is sprayed into the roll bite at a high pressure. The direction of the oil is such that it flows from the center sidewise and thereby washes away any fragments of the metal that may detach themselves from the strip. This is essential for the edges, which if slit, would tend to have a lot of small detachable particles.

Sendzimir Collapsible Winders: The Sendzimir mill uses heavier tension than conventional mills for reducing the strip, which is especially needed for harder metals, such as stainless steel, titanium, and various alloys. These heavier tensions generate much heavier collapsing pressure on the winder block, making it necessary to develop a new design.

The Sendzimir collapsible winders are made either of a single, double, or multiple pyramid. This design differs from the conventional type design as the drum itself is virtually filled with solid metal when it is in expanded position and the strip is being coiled.

On the double pyramid winder, the outer pyramid is actuated by the inner shaft and moves a full stroke to the front of the mill. By means of suitable levers, the covers which fit these pyramids move one-half of that distance and collapse neatly between the pyramids. This design is symmetrical when viewed from the front and results in concentric running of the winder even after considerable use.

On the quadruple pyramid winder, the inner shaft pushes the outer elements forward and brings them to collapse. The pyramids themselves are attached to the gear and do not move.

These collapsible winders prove extremely valuable in service, especially when high tensions are used and where the coil build-up ratio is 2.5:1 or even 3:1. Furthermore, if thin gage material is rolled, then the number of wraps on the winder is tremendously increased, and the collapsing force increases in logarithmic power.

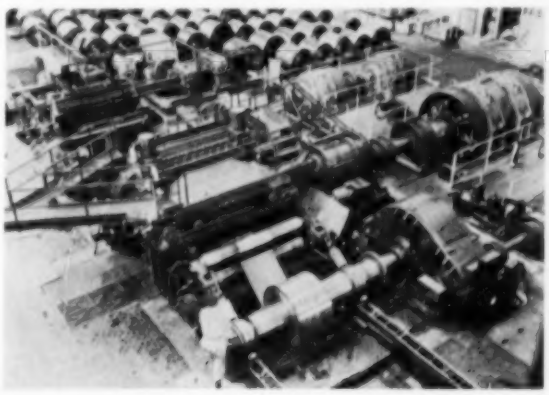
Industrial Application of the Mill

Stainless Steels: The greatest prominence achieved by the Sendzimir mill is in the stainless steel field. The mill has been adopted as a standard tool by nearly every large stainless steel plant for rolling flat products. Aside from the U. S., Sendzimir mills are used in England, France, Italy, Germany, Sweden, and Japan for rolling stainless steels.

The ability of the mill to reduce even the hardest austenitic grades of stainless steel from hot rolled gages down to the finished strip has saved numerous intermediate anneals and reduced production costs. Aside from ferritic and austenitic grades of stainless steel, the mill also rolls steels containing up to 12 pct Mn, which work-harden rapidly and steels containing 17 pct Cr, 5 pct Ni, and 5 pct Mn.

Heavy reduction without intermediate annealing causes the surface to become more dense and lustrous. Tungsten carbide rolls can be furnished with mirror finish and used for the final passes. These rolls can be used for long periods without regrinding. In order to brighten the surface and bring back the highest quality surface finish, these rolls can be sprayed with a diamond powder suspended in oil and lightly polished.

Low Carbon Steels: In the early days of the Sendzimir mill, its use was exclusively confined to the rolling of low carbon steel. In Europe, the mill is used to produce strip characterized by gage variations less than 0.0004 in. across the width and hardly any gage variation from one end of the coil to the other. This feature eases subsequent cold working and fabricating operations. In addition, it enables the strip to be slit into narrow strip, each of the



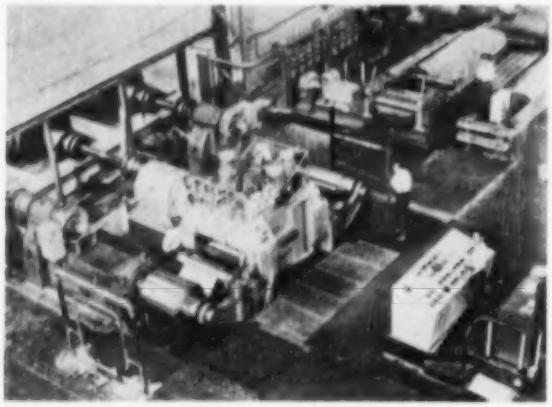
In France

At the Isbergues plant of the Compagnie des Forges de Châtillon, Commentry et Neuves-Maisons this ZR 22-80 Sendzimir mill of the 1-2-3-4 design rolls steel strip up to 80 in. width. Rolls may be changed in a matter of less than a minute, permitting a fresh set of rolls to be used on finishing passes.

same gage and flatness. The Sendzimir mill has also filled an important gap in the production of very thin gage low carbon strip. These gages are below those of tinplate and are used a great deal for shim stock.

With respect to gage variation from one end of the coil to the other, it has been claimed by certain German and English rolling mill experts that with a small diameter work roll, the variation in the gage during acceleration or deceleration of the mill is proportionally less with the reduction of the work roll diameter. This is evident with the Sendzimir mill, where gage variations during these periods is only a fraction of that on conventional mills.

The Sendzimir mill can not only keep the whole coil within the commercial tolerances, but also be so used that the tolerance on the finished product is substantially reduced. On conventional mills, no matter whether reversing or tandem, it is customary practice to reject or reclassify the first two to four sheets that are cut from each end of the coil. These sheets are above the 10 pct commercial tolerance allowed by American standards.



This ZR 22-49 Sendzimir cold strip mill of the 1-2-3-4 design is used to roll medium carbon steel at the plan of S. A. Métallurgique d'Espérance-Longdoz at Liège, Belgium.

The Sendzimir mill located at Espérance-Longdoz in Liège, Belgium, can keep within 100 pct of the commercial tolerance of ±7 pct. The mill has been compared with three 4-high standard mills in that vicinity of similar vintage. It was found that one of them was able to keep this 100 pct tolerance, but the other two fell to 98 pct and 95 pct. If this tolerance is reduced to ±4 pct, the Sendzimir mill can keep, on the average, 95 pct of the material within these tolerance limits; however, the respective 4-high mills fall to 68 pct, 65 pct, and 57 pct.

This Sendzimir mill for low carbon steel at Espérance-Longdoz has been producing over 20,000 metric tons of low carbon steel per month. Production is from hot rolled gages to an average recomputed gage of 1 mm. In normal practice two or three passes of up to 50 pct reduction per pass are required, depending on the width and thickness of the hot rolled steel. The mill has a base speed of 750 fpm (full torque speed of mill motor) and a top speed of 1500 fpm; the mill motor is 2500 HP.

Copper and Brass: Most brass and copper producers in the U. S. have a Sendzimir mill for rolling thin gage radiator brass or for making close tolerance material. Mill rolls can be changed in only 45 sec, and this helps the operator to use a fresh pair of work rolls for the finishing pass in order to give the metal the desired finish.

Aluminum: The Sendzimir mill is used to roll soft aluminum as well as alloys containing magnesium. The latter work-harden more rapidly and tend to edge crack. Such grades of aluminum as 52S and 56S that contain 3 and 5 pct Mg respectively, are especially difficult to roll. The small diameter Sendzimir work rolls tend to eliminate the stresses in the work roll bite, particularly near the edges. With conventional mills these alloys would have to be either annealed or the edges slit in order to continue rolling.

This ZR 32-4 Sendzimir mill of the 1-2-3-4 type rolls Ni-Fe alloys and silicon steel to 0.00009 in. thick. It is in operation at the Marengo, Ill., plant of Arnold Engineering Co., a subsidiary of Allegheny Ludlum Steel Corp. The ultra-thin metal produced on this mill shows great promise in electronics.

Silicon Steel: During the last few years the Sendzimir mill has been used more and more for rolling silicon steel, especially oriented grain qualities. The Sendzimir mill can easily take the first pass reduction on the hot rolled strip of upwards of 40 pct, and it can reduce the number of passes that are needed on the standard mills. On thin gage material, one Sendzimir mill rolls annealed 0.013-in. thick strip on the first pass to 0.003 in., 77 pct reduction.

Other Metals: Sendzimir mills have been used for a variety of other metals. Some mills have rolled gold and silver; other mills have rolled bi-metals for jewelry trade as well as for electrical contacts. The mill has been used for rolling metallic molybdenum and watch main-spring material.

The Sendzimir mill has also been successful in reducing commercially pure titanium and titanium alloys. The mill has reduced commercially pure titanium from 0.036 in. to 0.006 in. without intermediate annealing, individual passes ranging between 10 and 25 pct reduction.

Future Developments

Higher speeds and higher powered mills are being built every day. Metals that could not be rolled at a given speed and reduction today, can and will be rolled at higher speeds and heavier reductions tomorrow. These advances can be attributed to improvements in metallurgy of the metal, to improvements in the metallurgy of the rolls, and to the improved quality of the lubricant and the way in which it is applied to the mill.

USSR:
Constant Humidity Blast Furnaces

Based on an Article

submitted by the Soviet Delegation to the Steel Committee of the Economic Commission for Europe, and

published in

***Advances in Steel Technology in 1955*, by the United Nations' Economic Commission for Europe, Geneva, Switzerland, 1956.**

Picture, courtesy The Iron and Steel Institute, London.

A delegation from the British steel industry described a visit to steel mills in the European USSR in 1955 in Special Report No. 57, *The Russian Iron and Steel Industry*, published by the Iron and Steel Institute, London. At the Zaporojstal works in the Ukraine, originally built on American design, all five blast furnaces were found to operate with top pressure and constant humidity. One such furnace, shown above, has a 1350 cu m working volume and a 26-ft hearth diam. It is blown under a pressure of 27 psi with rates up to 100,000 cfm and a top pressure of 5 psi. The humidity is automatically controlled at 20 g per cu m by steam injection. The burden, averaging 41 pct Fe, is made up of 80 pct sinter. Such furnaces produce up to 1750 long tons per day with an average coke consumption of 17½ cwts per ton of iron.

ONE condition essential for regular, high, blast-furnace output is to maintain constant all factors having any influence on the characteristics of operation. Until recently, the humidity of the wind was never a constant factor, and it varied considerably, from some tenths of gm per cu m to 20 gm and above, according to the season and the time of day.

By injecting water vapor into the blast furnace wind it is possible to regulate the heat and the speed of the furnace operation. The dissociation of water vapor in the crucible requires a certain quantity of heat which can be supplied by higher blast temperatures. Dissociation also increases oxygen and hydrogen content of gases at the crucible level.

Experiments on the humidification of blast furnace wind were carried through with success in 1939 and 1940 at the works of Kouznetsk in West Siberia, Magnetogorsk in the Urals, and Novo-Lipetsk. Again in 1950 and 1952, tests were made on the influence of humidity on blast furnace operation and the characteristics of the iron and steel produced.

The results showed that the production of the blast furnace increased with an increase in wind humidity, provided that the loss of heat due to the decomposition of the water vapor was compensated by an elevation in the blast temperature. With an increase in the water vapor content of 10 gm per cu m of air, the blast furnace production increased from 3.5 to 4.5 pct, and the coke rate decreased by as much as 1.5 pct. At the same time, the oxygen content of the gases at the crucible level increased by 0.63 pct, and hydrogen increased by 1 pct.

Thus, with the increased reducing power of the furnace gases, the speed of the operation is stepped up. The water injection of 10 gm per cu m raises top temperatures on the order of 90°C, but this does not exercise any unfavorable influence on the operation of the furnace, for the temperature at the crucible level remains practically unchanged.

Prior to the introduction of water vapor into the blast furnace wind, it is reported that the average humidity of the wind of blast furnaces in the USSR was 7.5 gm per cu m. At present, on blast furnaces with water vapor injections, the blast is automatically maintained between 20 and 25 gm per cu m, depending on the stove capacity. This results in increasing production by 7 pct and decreasing the coke rate by 2 pct. Injections are made prior to the hot blast stove.

The use of humidified blast does not increase production costs, for the direct costs involved with the utilization of water vapor and the increased quantity of blast furnace gas necessary to heat the stoves is compensated by the lower coke rates and the increased production rate.

The influence of humidification on the hydrogen content of pig iron and steel, on the tendency toward flaking, and on the mechanical characteristics of the metal has been studied in many plants situated in regions having widely varying weather conditions. The results show that furnaces can operate up to 30 gm of water vapor per cu m of blast without experiencing any increase in the hydrogen content of pig iron. With steel produced from pig iron from humidified air blast furnaces, there is no noticeable tendency toward an increase of flaking in the case of rails and medium carbon steels.

Many years of trials on a large number of blast furnaces at different plants corroborate the initial results and show that the humidification of wind permits an important increase of blast furnace production, without accompanying technical or economic problems concerning either the operation of the blast furnace or any change in the quality of the steel.

Based on an Article by G. Bruckner, Published in

Advances in Steel Technology in 1955 by the United Nations' Economic Commission for Europe in Geneva, Switzerland, 1956.

Lignite Coke in Low-Shaft

Experiments Show Ability to Operate Low Shaft Blast Furnace on 100 pct Lignite High Temperature Coke

IN central Germany, a belt of low grade iron ores and lignite deposits extend across what is now the boundary between the German Federal Republic and Eastern Germany. During the Hitlerian era these iron ores were extensively exploited after construction of the vast Herman Goering works at Salzgitter. Further to the east, at the town of Calbe, experiments were conducted on smelting these ores in a low shaft blast furnace.

Following World War II, little was done with the Calbe project until 1951 when the drive for the industrialization of Eastern Germany required quantities of iron for castings. Two low shaft blast furnaces, probably making use of some existing equipment, were put into operation, and within a few years ten low shaft furnaces were in operation, together producing 700 tons of foundry iron per day.[1]

Eastern Germany is essentially devoid of deposits of good coking coal and is largely dependent upon imports from Poland.[2] There are, however, some deposits in the vicinity of Zwickau and Dresden, but these are totally inadequate to meet the demands of the economy. The area does possess large lignite deposits, and, in recent years, experimental work has been done on coking these materials. For several years now a lignite high temperature (HT) coke has been produced at the coke-ovens in Lauchhammer in Selesia.[3] The paper by Herr Brückner describes some of the results obtained when using this coke in the low shaft furnaces.

G. BRUCKNER is an engineer in the Eastern Zone of Germany.

Table I. Analyses of Coke from Zwickau and Lignite High Temperature Coke Used at the Maxhütte Plant in Eastern Germany

	Ash, Pct	S, Pct	Volatile Matter, Pct	Moisture, Pct	Lower Calorific Power
Zwickau Coke	9.79	1.31	1.02	6.5	6,411
Lignite Coke*	11.57	1.63	1.06	4.53	6,522

* This lignite coke was 83 pct + 40 mm and had a coefficient of abrasion resistance of 47 pct.

Experiments with lignite coke began in 1952 at the low shaft blast furnace at the Maxhütte, a small integrated blast furnace-steel mill, formerly operated by Flick interests. The furnace has an operating capacity of 40 cu m; it uses 17 tuyeres of 100 mm interior diam; and the wind temperature is between 700° and 800°C. The first series of trials of lignite coke were far from satisfactory. The coke, although sufficiently resistant, resulted in extremely slow furnace operation. This was explained by the non-porous character of the coke, and methods were sought to modify its properties. Coking temperatures were reduced, and thermal shock at the beginning of cokefaction was minimized.

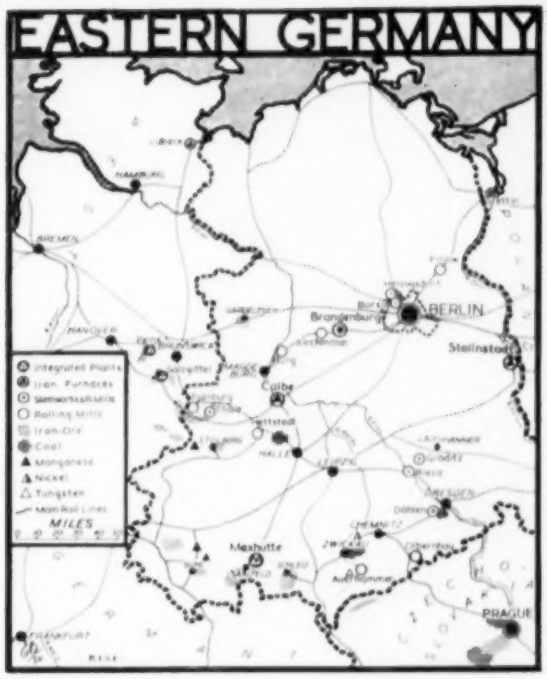
With the improved lignite coke, a second series of trial runs was undertaken to determine the optimum charging ratio of lignite coke to the previously used Zwickau coke. Table I shows the physical and chemical properties of the lignite coke as well as the Zwickau coke.

When lignite coke was charged up to 65.5 pct of the total coke, the same output of pig iron was obtained as when using completely Zwickau coke. Wind volume was less than used previously, because less coke was consumed, due to the higher calorific power of the lignite coke. However, when this material was increased beyond 65.5 pct, pig iron production dropped off regularly, and coke consumption

Table II. Operating Data of Low Shaft Blast Furnace at Calbe in Eastern Germany, April to October 1955

Production of lignite coke to total coke charged	0	71.00	82.00	100.00
Production of foundry pig iron, tons per 24 hr	71.40	77.60	78.00	81.00
Consumption of coke, tons per ton iron produced	2.207	1.727	1.665	1.497
Efficiency of charge, pct	23.10	25.60	26.00	25.00
Fe in charge, pct	23.30	24.20	24.20	24.20
Composition of pig iron				
C	3.43	3.64	3.58	3.72
Si	2.46	2.60	3.05	2.92
Mn	0.59	0.60	0.73	0.82
S	0.123	0.086	0.066	0.048
Temperature of pig iron, °C	1,261	1,282	1,279	1,288
Slag index, $\frac{(CaO + MgO)}{(SiO_2 + Al_2O_3)}$	1.09	0.96	0.93	1.01

During World War II the steel industry of Eastern Germany was producing at the rate of 1.8 million metric tons of ingots. Output was largely based on scrap, for the area lacked sufficient raw materials, and only the Maxhütte operated blast furnaces. The dismantling program reduced ingot capacity to some 200,000 tons, but most plants were subsequently rebuilt with expanded capacity. A new integrated plant, based on Russian ore and Polish coke, was commenced at Stalinstadt on the Oder River. Foundry iron was supplied by ten low-shaft blast furnaces at Calbe. According to the United Nations, 1955 Eastern German production was 1.5 million metric tons of pig iron, and 2.5 million tons of ingots . . . Map, courtesy British Iron and Steel Federation.



Blast Furnace

increased, although the wind volume remained lower than previously. This is explained by the irregular combustion of coke which imperils the thermal balance of the low shaft furnace.

These experiments demonstrated that a low shaft blast furnace can function with a large portion of lignite coke, but results also showed that further gains could be made; so efforts were again made to improve the properties of lignite coke.

Between April and October 1955, trial runs were begun at Calbe utilizing the new lignite coke. The object of these experiments was to ascertain if it were possible to operate low shaft furnaces on 100 pct lignite HT coke. The furnaces at Calbe have an operating capacity of 70 cu m; they utilize 14 tuyeres of 120 mm diam; and the pressure and temperature of the wind were maintained constant at 280 mm Hg and 650°C, respectively.

The operation was carried on during the course of normal operations, producing a type of foundry iron called "Deutsch I", with iron ore from Badeleben and Büchenberg, basic manganese slag, lime rock, scrap, gas plant coke, and lignite coke. Table II shows the results; averages are for a seven month period.

The operation at Calbe demonstrates that by increasing the quantity of lignite coke in the charge, the pig iron production was influenced favorably both in respect to quantity and quality. By reason of the higher calorific power of this material, as well as the basic ash from the coke, it has been possible with a charge using exclusively lignite coke to reduce by 719 kg the quantity of coke charged per ton of pig iron produced. After taking account of the changes in charge efficiency, pig iron production was increased by about 5 pct. The temperature of the pig iron poured was higher in spite of the lower coke consumption, and the pig iron contained less sulfur, even when the slag was slightly more acid.

The lignite coke used in these experiments was composed of almost equal parts of pieces 30 to 45 mm and pieces of more than 45 mm. On the other hand, the coke used at the Maxhütte was composed of 83 pct +40 mm; thus, it may be assumed that the presence of the smaller fraction of coke particles at Calbe, exercised a favorable influence on the operation of the low shaft furnace and on its thermal balance. Future experiments will be carried out to verify this assumption.

In conclusion, it has been ascertained that a low shaft blast furnace can burn exclusively lignite coke and that better metallurgical results are obtained than with the gas plant coke previously used.

The following are the economic arguments in favor of the utilization of lignite coke: a) because of its higher calorific values, it is possible to increase the iron:coke ratio and reduce coke consumption by 15 pct; b) with its basic ash, it is unnecessary to burden the furnace with limestone to flux the coke ash; c) the quantity of limestone necessary to flux sulfur in the ash is less than in the case of petroleum coke; the low shaft furnace can thus function with a less voluminuous acid slag; and d) the moisture content of lignite HT coke is lower.

Recent Experiments Show Adaptability of Lignite Coke to Conventional Blast Furnace

At the present time, the substitution of lignite HT coke for normal coke in the regular size blast furnace is the object of experiments still being conducted. From the results thus far obtained, it has been determined that a mixture of 20 pct lignite coke does not change the production of the 450 cu m blast furnaces at Maxhütte. A 700 cu m blast furnace at the new plant at Stalinstadt on the Oder River, has been operated without difficulty on a charge containing 15 pct lignite HT coke.

These experiments are of great importance to many regions where steel industries are being built up or expanded without adequate supplies of coking coals but where there are plentiful lignite deposits.

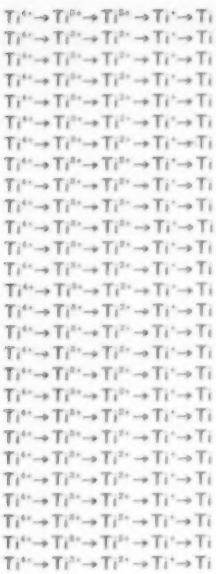
References by Editor

[1] *Stahl und Eisen*, 1954, vol. 74, No. 20, p. 1321.
[2] *Stahl und Eisen*, 1952, vol. 72, No. 23, p. 1498.
[3] *Stahl und Eisen*, 1953, vol. 73, No. 17, p. 1135.

The Current Status of Research and Development on

Electrolytic Titanium

by M. E. Sibert and M. A. Steinberg



EFFORTS have been made to prepare titanium metal by electrolytic methods for almost as long as the element has been known, but the recent increase in demand has accentuated these efforts. Although much of the work has not yet been published, there is a significant amount of scattered information available in technical and patent literature. It is the purpose of this paper to organize and evaluate the published information together with work done at Horizons Inc.

Aqueous and organic electrolytes will not be considered in this review, since it has been adequately demonstrated[1, 2] that such procedures can only yield, at best, reduced titanium compounds, primarily oxides. Thus, this discussion will concern only the electrolysis of titanium compounds in fused salt media. Literature on fused salt electrolytic titanium has principally concerned oxides and halides as source electrolytes, and a third process, for soluble anodes, which is primarily for refining.

Electrolysis of Titanium Oxides

Literature Background: Fig. 1 is a typical titanium deposit obtained from the electrolysis of TiO,[3] one of the few oxide procedures yielding metallic deposits. Oxides of titanium, in particular TiO_2, have always been attractive starting materials for an electrolytic procedure. The dioxide is mined as such—rutile—and reduced oxides are obtained by chemical reductions.

Borchers and Huppertz proposed an electrolysis of TiO_2 dissolved in molten anhydrous alkaline earth halides in 1903.[4] The oxide was added to the melt either prior to or during electrolysis. Deposition of titanium metal was claimed according to the reaction: $TiO_2 + 2M \rightarrow 2MO + Ti$. In light of equilibria considerations, it is doubtful that pure titanium was prepared by this method.

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Skylarenko and Lipkes[5] have reported the following experimental results dealing with TiO_2 electrolysis: 1) 1.2 moles of TiO_2 were added to 7 moles of $Na_4P_2O_7$ and 15 moles of NaCl in a porcelain container and electrolyzed using graphite electrodes. No metal was produced. 2) TiO_2 dissolved in cryolyte was subjected to electrolysis, but no titanium was deposited. 3) The Borchers-Huppertz procedure ($CaCl_2$-TiO_2) was checked. Titanium bearing materials were prepared, but maximum titanium content was 89 pct. Such deposits were largely lower oxides. This procedure was also tried with addition of NaCl to lower the melting point, but this was unsuccessful. The U. S. Bureau of Mines[6] also failed to prepare the metal using TiO_2 in $CaCl_2$ and TiO_2 in an NaF-KF mixture.

More recently, a patent has been issued concerning electrolysis of TiO_2 in melts under an inert atmosphere.[7] Suggested melts are alkali metal phosphates, borates, or fluorides. These may be used singly or mixed with each other. The melt is operated containing 5 to 10 pct of dissolved TiO_2. Titanium bearing deposits are claimed, but no properties of the metal are given.

The authors have disclosed and patented a procedure for electrolytic reduction of TiO in fused melts.[8, 9] The TiO is dissolved in $CaCl_2$, and titanium metal is cathodically deposited, although the product is not of sufficient purity for most commercial applications. A further patent[10] deals with the deposition of titanium from melts of TiO, K_2TiF_6 and NaCl. It is probable that the purity of metal prepared by these methods could be notably improved by use of liquid cathodes which would not dissolve oxides, carbon, etc., but would dissolve an appreciable amount of titanium.

Melts of TiO_2 in $CaCl_2$ were also subjected to electrolysis;[3] TiO of reasonable purity was prepared in this manner, but total reduction was not achieved.

The Carlysle process utilizing a TiO_2 feed material has been reported[11] but few details are avail-

able. The preparation of high purity titanium at 98 pct efficiency is claimed from a cell operating at less than 400°C.

Gleave et al.[12] report the preparation of electrolytic titanium from TiO_2, titanates, and rutile dissolved in a fused melt such as $CaCl_2$. Electrolysis is carried out using a liquid metal cathode, such as Zn, Cd or Pb, in which titanium is soluble. A 99.5 pct Ti product was reported obtained from a 2.5 pct Ti-Zn cathode produced by this process.

Process Evaluation: The approaches to electrolytic reduction of titanium oxides show considerable similarity. The only instances in which the product approached pure titanium utilized an inert atmosphere. In no case has a commercially pure product been prepared in this manner. The report on the Carlysle Process[11] is not confirmed, and this may not be a direct oxide electrolysis.

General requirements for a process of this type are the following:

1) Use of TiO, Ti_2O_3, Ti_3O_5 or TiO_2 as source electrolyte.

2) Oxide employed should be soluble in melt or anodically active to yield soluble Ti halides. Generally, oxides are relatively insoluble in alkali chloride melts, and only slightly soluble in alkaline earth chlorides. They do dissolve in fluoride melts to a greater extent, as shown in Table I. In the case of insulators like TiO_2 in the presence of carbon, an oxide can be forced into solution by halogenation. In the case of conductors such as TiO, an oxide anode will do it.

3) The melt produced must be electrically conductive. This is not a problem in any reported case of this type.

4) An inert atmosphere must be employed to protect both reduced titanium species in the melt and deposited titanium from oxidation.

5) Reduced species should have minimum contact with the anode to avoid possible reoxidation, e.g. $TiO \rightarrow TiO^{2+} + 2e$. The use of suitable diaphragms or other isolation of the anode would be desirable.

6) Anode gases should not contact the cathode. In most cases a halogen and/or CO and CO_2 is produced at the anode. Such gases can react with deposited titanium.

7) In the case where oxides are directly dissolved, the reduction, electrolytic, or chemical, must go substantially to completion to obtain a titanium deposit of satisfactory oxygen content. There is some doubt whether equilibria of such reactions are sufficiently favorable for this use.

8) Current must be passed at a voltage greater than the decomposition voltage of the compound being decomposed.

Attempts to electrolytically reduce TiO_2 and other oxides to metal in alkali chloride melts have met with failure.[5, 6] This is ostensibly due to the very low miscibility of the oxides with such mixtures. The only promising results have been obtained using a $CaCl_2$ electrolyte. Some success has been reported using fluoride melts, but these are difficult to handle in respect to recovery of metal due to their water insolubility. Favorable results have been reported using phosphate and borate melts,[7] but no details are available concerning these procedures.

Reduction processes for oxides may take place electrolytically or by means of an active metal secondary reduction, e.g. Ca, but it is improbable that the reduction can proceed to completion. The only promising alkaline earth melt has been $CaCl_2$ but generally, use of alkali chlorides has not been successful. Recent work indicates that calcium is incapable of complete reduction of Ti-O alloys. Studies on equilibrium relationships of these alloys with liquid calcium show the minimum oxygen value obtainable to be relatively high. McClaine[13] reports a value of 0.136 pct at 1000°C. Kubaschewski and Dench[14] have also reported on deoxidation of titanium with calcium, magnesium, and barium. Minimum oxygen contents were obtained as follows: Ca-0.07 to 0.12 pct; Mg-1.5 to 2.8 pct; Ba-ca. 6.7 pct. This is accounted for by the equilibrium diagram for titanium-oxygen where the $\beta + \alpha$ region extends from 1.6 to 2.1 pct at 1000°C; at 1200°C it runs from 1.4 to 3.6 pct. The presence of iron raised these values considerably. Such values indicate that pure titanium is unlikely to be produced by an alkaline earth metal reduction. Worner and Cordner[15] have previously expressed this opinion.

Table I. Solubility of Ti Compounds in Fused Salts

Compound	Salts							
	LiCl	NaCl	KCl	LiF	NaF	KF	$MgCl_2$	$CaCl_2$
TiO	vss	vss	vss	ss	ss	ss	ss	ss
Ti_2O_3	ss	ss	ss	s	s	s	s	s
TiO_2	i	i	i	s	s	s	ss	ss
TiF_2	vss[1]	ss[1]	vss[1]	—	—	—	—	—
TiF_3	s	s	s	s	s	s	—	—
TiF_4	s	s	s	s	s	s	s	s
$TiCl_2$	s	s	s	—	—	—	—	—
$TiCl_3$	s	s	s	s	s	s	s	s
$TiCl_4$	vss	vss	vss	—	—	—	vss	vss
K_2TiF_6	s	s	s	s	s	s	s	s

Symbols: s = soluble >5 pct
ss = slightly soluble <5 pct
vss = very slightly soluble <1 pct
i = insoluble
[1] = as complex

Electrolysis of Titanium Halides

Literature Background: Existing literature on titanium halide electrolysis concerns fluorides and chlorides exclusively, with only casual reference to bromides and iodides. This is largely due to the fact that the latter are usually prepared either from the tetrachloride or by direct halogenation, and in either event are relatively costly starting materials offering little commercial promise.

Probably the earliest claim to production of ductile titanium by halide electrolysis is that of Kratky and Bruckner.[16] A salt of the metal was added to a previously electrolyzed melt consisting of salts of more electropositive metals. Thus, the procedure was in reality a chemical reduction or an indirect electrolysis. The preparation of ductile Ti, W, Mo, U, V, and Ce was claimed.

Fischer and Dorsch[17] employed titanium halides in fused alkali or alkaline earth halide melts to prepare electrolytic titanium alloys. The cathode consisted of the metal to be alloyed with titanium, and the anode was of graphite. Alloys containing up to 45 pct Ti were claimed. Current densities at the cathode were specified as 800-2000 amps per sq m.

Many of the early patents of F. H. Driggs et al. refer indirectly to this type of electrolysis,[18] although most are specific to other metals such as uranium.[19] These specifications refer to electrolysis of refractory metal halides and double halides dissolved in alkali-alkaline earth halide melts.

Skylarenko and Lipkes[5] reported on the electrolysis of K_2TiF_6 in molten NaCl-KCl, but substan-

tially negative results were obtained. A typical run was carried out at 820°C under 300 amps per sq dm cathode current density giving a product containing 76.3 pct Ti. All of this work was carried out in air, and deposits were principally reduced oxides.

More recently the Skylarenko-Lipkes procedure has been carried out in an inert atmosphere[26] producing dendritic titanium crystals. Electrolysis was run at 850°C under 30-100 amps per sq dm cathode current density. Similar results were reported using $TiCl_4$ and TiF_4 in place of K_2TiF_6.

Drossbach reported metal deposition in a similar manner from melts of K_2TiF_6-LiCl-KCl and K_2TiF_6-NaCl-KCl.[27] This was an electrode reaction study, however, and the work was carried out in air within glass systems, so the results are not directly applicable to process work.

A further report of successful titanium deposition from this type of melt[28] was carried out at 550°C under hydrogen using $TiCl_4$ in KCl-LiCl.

Table II. Properties of Melt Constituents

Compound	M.P. °C	B.P. °C	Liquid Density g per cc. 850°C	Form	Sp. Elect. Cond. 850°C	E_D Volts 850°C
TiF_3	—	—	—	vlol. cryst.	—	>1.0
TiF_4	s	284	2.798[20°]	cl. solid.	—	>1.5
K_2TiF_6	780	—	—	cl. cryst.	ca. 3.7	1.7
$TiCl_2$	677	1477	—	blk. cryst.	—	—
$TiCl_3$	927	d 440	—	vlol. cryst.	—	—
$TiCl_4$	−30	136.4	1.726[20°]	cl. liq.	nil	—
LiCl	613	1353	1.394	cl. cryst.	7.66	3.24
NaCl	801	1413	1.505	cl. cryst.	3.77	3.15
KCl	776	s 1500	1.480	cl. cryst.	2.36	3.29
$MgCl_2$	708	1412	1.646	hyg. cryst.	1.33	2.47
$CaCl_2$	772	>1600	2.03	hyg. cryst.	2.34	3.15
$SrCl_2$	873	—	2.71	hyg. cryst.	—	3.20
TiO	1750	—	4.92[20°]	bronze cryst.	—	2.6
Ti_2O_3	2130	—	4.6[20°]	vlol. cryst.	—	—
TiO_2	1640	—	4.26[20°]	cl.	—	2.05

A more complete evaluation[29,30,31] of this general type of procedure has been given in disclosure of a method for preparation of high purity titanium by electrolysis of K_2TiF_6 in molten NaCl. This electrolysis is generally carried out at 750°C using graphite anodes and steel cathodes. Starting cathode current density is 350-550 amps per sq dm. Large flake like crystals and coarse granules are produced, corresponding in purity to Kroll process material, shown in Fig. 2.

The Shawinigan Water and Power Co.[32] has been granted a patent on a process employing $TiCl_4$ as a source electrolyte. A melt of NaCl, KCl, $CaCl_2$, or $MgCl_2$ is employed, and $TiCl_4$ is distilled into a bell covered cathode compartment. The $TiCl_4$ is reduced to lower chlorides and finally to metal, possibly by secondary reduction through electrolytic deposition of the active metal constituent. The process operates at 675°C and produces 99.2 pct purity Ti. A 38 pct current efficiency was reported.

Work at this laboratory has employed $TiCl_4$ in a melt of K_2TiF_6-NaCl[17]. The $TiCl_4$ reacts with K_2TiF_6 to yield a stable reduced electrolyte; the original K_2TiF_6 is not decomposed during electrolysis.

The Imperial Chemical Co. Ltd.[33] has also patented a chloride process wherein a mixture of H_2 and $TiCl_4$ is fed into the alkali or alkaline earth chloride melt. This process operates at 800°C.

Another modification of the $TiCl_4$ procedure is that of the Titan Co. Inc.[34] In this procedure a bath of 73 pct SrCl, and 27 pct NaCl is recommended (m.p. 560°C). Two cathodes are employed: a solubilization cathode and deposition cathode. The $TiCl_4$ is introduced at the solubilization cathode and reduced to $TiCl_3$ and $TiCl_2$. These products are then transferred to the deposition cathode and further reduced to metal. Through use of a porous diaphragm, contact with the anode is minimized. A further Titan patent[35] involves the same $SrCl_2$-NaCl melt, but it employs $TiCl_4$ introduced below the surface of the melt and preferably near or through the cathode. The process operates at about 700°C using a cathode current density of 1-6 amps per sq cm. Preparation of 99.9 pct Ti of BHN 130 is claimed with a yield of 80 pct.

A Japanese patent[36] also uses $TiCl_4$ as a source material for electrolysis. A melt of KCl, CaF_2, $MgCl_2$, and $TiCl_4$ is specified, the $TiCl_4$ being introduced through perforated cathodes. A Ti-Mg mixture collects at the bottom of the cell. This is vacuum treated to yield titanium of VHN 225.

A recent report[37] considers the electrolysis of $NaTiCl_4$ in KCL-NaCl with additions of K_2TiF_6 and K_2SiF_6. Variable results were reported, in most cases yielding metallic cathode deposits. No reports on purity were given.

Process Evaluation: A survey of reported halide electrolysis procedures immediately suggests several similarities. In most cases, $TiCl_4$ or K_2TiF_6 is the starting material. Since $TiCl_4$ is soluble to a very small extent in fused salt melts (Table I), the reported procedures entail an in situ or prior reduction to $TiCl_3$ and/or $TiCl_2$. Thus, all reported procedures deal with electrolysis of $TiCl_2$, $TiCl_3$, TiF_2, TiF_3, TiF_4, or complexes thereof.

In all the reported procedures for chlorides or fluorides, chlorine gas is the anodic product, even though fluoride ion may first be liberated. Fluorine gas immediately and quantitatively liberates chloride ion from solution.

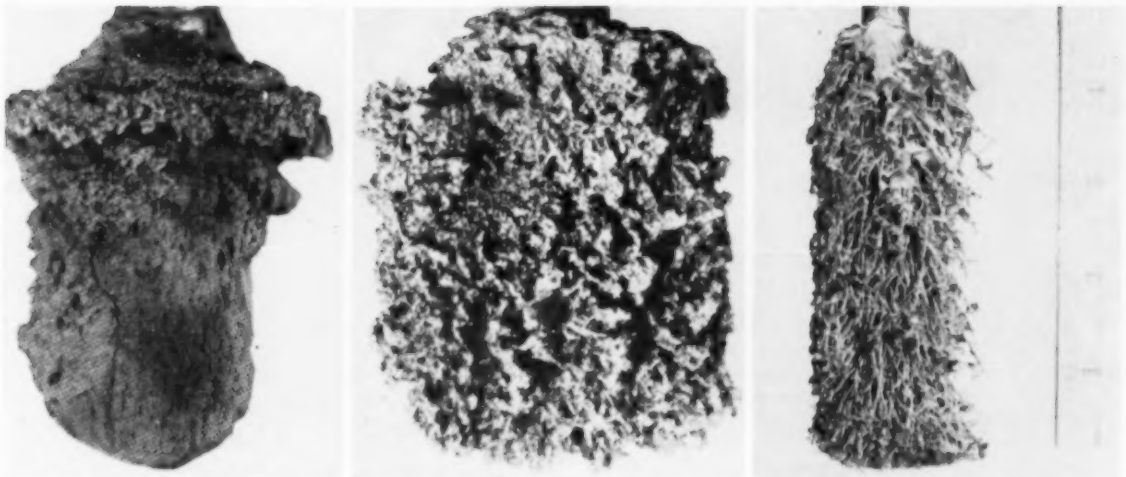
Alkali or alkaline earth chlorides are almost universally employed as solvents for titanium halides, due to solubility considerations shown in Table I.

In the case of K_2TiF_6 (TiF_4) electrolysis, it has been shown[38] that the reduction proceeds stepwise from $Ti^{++++} \rightarrow Ti^{+++} \rightarrow Ti^{++} \rightarrow Ti$. It appears likely that the chloride reduction takes place similarly, and that therefore, in principle, all reported halide processes reduce to the same general mechanism.

The Electrolyte: Choice of electrolyte is one of the more important considerations in this approach to titanium preparation. It appears that any of the titanium chlorides or fluorides noted can be successfully reduced to metal. These are also appreciably soluble in the alkali and alkaline earth halides.

Not previously noted was TiF_2 which in theory is indicated to be thermodynamically unstable. However, recent evidence indicates that this material does exist at 700-1000°C,[17,38] at least in a complex form. Drossbach[27] has stated that TiF_2 is only slightly soluble in NaCl-KCl. Only very small amounts of Ti^{++} are detected in an NaCl-K_2TiF_6 melt, substantiating the insoluble nature of this material.

Of the possible starting materials, the divalent halides have the lowest vapor pressure and would thus suffer the least loss from a high temperature melt. Losses of $TiCl_4$ and K_2TiF_6 are appreciable at 700-800°C over a period of time. The divalent salts are the most difficult to handle, being extremely reactive toward air and moisture. The most commonly employed starting materials have been $TiCl_4$ and K_2TiF_6. The former is easily distilled into a melt and reduced to Ti^{+++} or Ti^{++}. The later may be



Left, Fig. 1, A typical cathode deposit of titanium from electrolysis of TiO in $CaCl_2$. Center, Fig. 2, shows the cathode deposit from the electrolytic decomposition of K_2TiF_6 in NaCl. Right, Fig. 3, shows the cathode deposit from a soluble anode fused chloride system.

handled in air as any normal salt at room temperatures.

Neither $TiCl_4$ nor K_2TiF_6 can be directly electrodecomposed to metal. $TiCl_4$ is non-conductive, and K_2TiF_6 readily polarizes the electrode. Pure K_2TiF_6 can be reduced to K_2TiF_4, but further reduction is very difficult without solution in another salt.

The Cell: The only specific requirements for a cell in this type of electrolysis are provisions for inert atmosphere operation and the use of non-corrosive materials of construction. No successful halide electrolysis has been reported where the cell was operated in air.

Graphite is the best known material of construction, although ceramics have been used with moderate success in all chloride systems, e.g., the diaphragms in the Titan cell.[28] Dense ceramics such as Al_2O_3, MgO, etc. are used. These are slowly attacked by titanium chlorides and more rapidly by fluorides. Where metal must be utilized, nickel is the most resistant to chlorine and chloride attack.

Many cell designs have been reported. The simplest and most common consists of a graphite crucible serving as one electrode and a central metal or graphite rod serving as the other. Several novel arrangements have been devised where $TiCl_4$ is employed. One patent[29] specifies a dual cathode cell, one for reduction of $TiCl_4$ to lower chlorides, and one for reduction to metal. Another[30] employs a bell over the cathode to prevent excess loss of $TiCl_4$ prior to reduction. Several examples of cell types used for electrolytic titanium are shown on page 1167.

Operating Conditions: Little has been published regarding the conditions most desirable for electrolytic titanium procedures. From what is available, the following would be indicated to be true for processes of this nature: 1) operated at 600-900°C; 2) high current densities are employed, ca. 10-500 amps per sq dm; 3) low Ti concentrations—3-6 pct; 4) use of high purity materials; 5) use of inert atmosphere, A or He. The relatively high operating temperatures, 600-900°C, are required by virtue of the melting properties of the constituents used in molten halide electrolysis. Some properties of commonly used materials are given in Table II. The minimum temperature useful for titanium preparation in such systems is of the order of 650-700°C.

A temperature increase has a favorable effect on particle size, but other effects are minor. The system and product are, of course, more susceptible to oxidation at higher temperatures. The majority of the melts employed fuse between 600 and 700°C. Addition of $TiCl_4$ and K_2TiF_6 tends to lower the system melting point, while $TiCl_2$ apparently tends to raise it.

Cathode current densities employed have most commonly been reported as 100-300 amps per sq dm. The high range is necessary in order to satisfy the voltage requirement (ca. 2V) with nominal electrode configurations, and also to obtain satisfactory current efficiency. Rapid elimination of chlorine gas tends to minimize the extent of back reactions.

Concentration of titanium ions in the melt has been reported in the range of 1-15 pct, but the most favorable operating level seems to be at 3-6 pct. The concentration is a mutually dependent function of both cathode current density and temperature. In general, chlorides seem to perform well at slightly higher concentrations than fluorides.

Relatively low titanium ion concentrations seem desirable from an efficiency standpoint. Highly concentrated melts are less conductive, and the tendency for reoxidation reactions to take place is greater.

As a general rule, the greater the purity of the starting materials and environment, the greater is the product purity, as demonstrated in the case of K_2TiF_6.[27] This is true where impurities concerned are soluble in a given melt, and decomposition voltage of the impurity is equal to or less than that of the substance being electrolytically decomposed.

Electrode Reactions in Halide Processes: In the halide processes reported, no definite mechanism of reaction has been established. However, certain points have been demonstrated, and the overall similarity of processes makes these of general interest.

In the case of fluoride procedures, it has been shown that the reduction is a stepwise electrolytic reduction: $Ti^{++} \rightarrow Ti^{+} \rightarrow Ti^{+++} \rightarrow Ti.$[27, 28] Drossbach[31] showed this to be true by polarization potential measurements on the system K_2TiF_6-KCl-NaCl. Kolk et al.[32] obtained similar results using voltammetric techniques on the system K_2TiF_6-NaCl. The reduction to Ti^{++} proceeds readily, and large amounts of trivalent salt are found in such melts. Divalent tita-

nium is detectable, but in much smaller amount.

It is likely that a similar reduction procedure takes place in the case of chlorides. Kellogg[35] et al. have shown that there is an equilibrium between $TiCl_2$ and $TiCl_3$ in NaCl-KCl. The ratio of $TiCl_2$ to $TiCl_3$ is approximately two at 700°C. The Titan patents[36, 37] have indicated that this is true as well.

Mellgren and Opie[38] have studied the Ti-$TiCl_2$-$TiCl_3$ equilibrium in $SrCl_2$-NaCl. In this case $TiCl_2$ is the predominant species—up to 88 pct. Dimer and trimer formation is suggested (Ti_2Cl_6, Ti_3Cl_8) to account for observed equilibrium constant values.

There are indications that materials other than the simple halides are involved in such a reduction, i.e., complex formation may be involved. The difluoride is, in theory, thermodynamically unstable, but its existence is indicated in fused systems. Materials such as K_2TiF_6 or $KTiF_5$ may be present rather than simple fluoride.

Work such as that described above has demonstrated that reduction, in the case of fluorides, is primarily electrolytic in nature. Previously, a number of authors have suggested that such reductions were, in reality, active metal reductions, the active metal being produced by electrolysis and in turn reducing the titanium salt. Metal fogs have been suggested as one way of picturing the system, as have metal vapor films.[27] Such active metal concentrations are present and undoubtedly do play a minor role in the reduction, but this is not the primary mechanism. Sodium and potassium are very soluble, for example, in their respective halides.[39, 40]

The anode reaction, corresponding to the stepwise reduction of a titanium halide, is chlorine liberation in all cases where an inert anode is used. Drossbach has offered proof for some CF_4 formation[41] in the case of K_2TiF_6 electrolysis with a graphite anode, but chlorine is the primary anodic product liberated.

None of the reactions involving titanium ions are 100 pct operative. The usual titanium cell efficiency is in the order of 50 pct.

The basic conflicting reactions causing reduction in current efficiency seem to be anodic reoxidations of Ti^{2+} and Ti^{3+} species. Under certain conditions $TiCl_4$ may be lost from the melt. Disproportionations are also undoubtedly involved in these reaction mechanisms, but they have no effect on efficiency.

A disproportionation reaction, which is operative in K_2TiF_6 procedures, is the reaction of the double fluoride with Ti and Ti^{4+} to form intermediate valence materials. Similar reactions take place where $TiCl_4$ is present. Reaction of this type with cathodically deposited metal appears to be only a minor factor.

Soluble Anode Procedures

Literature Background: A third general type of electrolytic procedure proposed for titanium is soluble anode fusion electrolysis. Such a procedure would have its primary application as a refining operation where off-grade titanium would be employed as an anode in a titanium bearing melt and deposited in purified form at the cathode. Success of a process of this type would depend on the difference in standard electrode potential of the titanium species from those of impurities to be separated.

As suggested by Kroll,[42] it is conceivable that extremely impure materials might be utilized in this fashion as soluble anodes. Such materials might include TiO, TiN, TiS, or Ti alloys. The previously discussed monoxide procedure[7] may be, to some extent, such a process. The TiO is introduced in a $CaCl_2$ melt where most of it settles to the bottom allowing for anode contact.

The Titan process has been modified for use as a soluble anode procedure.[36] The melt is similar to that specified for the decomposition process and contains 40-70 pct of $TiCl_2$. A typical melt of $SrCl_2$-NaCl-$TiCl_2$-$TiCl_3$ is operated at 700°C under 1 amp per sq cm cathode current density. Metal hardness was reduced from BHN 361 to 204.

More recently, a titanium refining procedure has been developed by the Bureau of Mines.[43] Impure titanium is made anodic in a simple alkali halide melt containing $TiCl_2$ and $TiCl_3$. The impure titanium is held in a steel basket or placed on the anodic crucible bottom. The melt is subjected to electrolysis in a steel vessel under an inert atmosphere, and titanium deposited in coarsely crystalline form at the cathode. A high degree of purification is reported. Success of the procedure is attributed to anodic retention of titanium oxides and other impurities.

This laboratory has also done extensive work dealing with titanium scrap purification and soluble anode procedures. A fusion electrolysis procedure has been successfully developed employing scrap titanium anodes in an alkali halide-titanium halide melt. The process is operated continuously in a graphite cell under an argon atmosphere. Titanium metal of hardness BHN 230 has been refined to give metal of BHN 80 by this method.

A typical cathode deposit produced from a chloride melt is pictured in Fig. 3. The material is in the form of large dendritic needlelike crystals and contains only about 30 pct by weight salts.

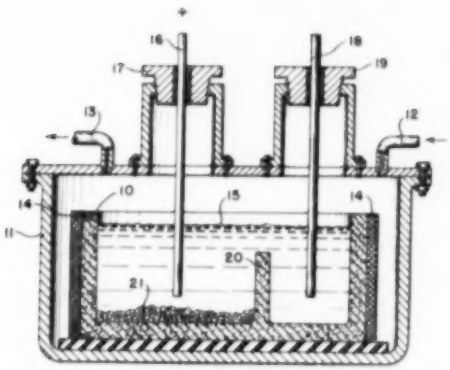
Process Evaluation: In general, fusion electrolysis soluble anode processes seem to behave similarly to ordinary electroplating processes. All reported processes employ a titanium halide carrier salt which is maintained at essentially constant composition in the melt. Titanium simply dissolves at the anode and plates at the cathode.

It has been indicated that titanium goes into solution in a titanium fluoride or chloride containing melt primarily as a Ti^{2+} species. This has been noted by voltammetric measurements at this laboratory[44] and has also been suggested by Drossbach.[45] It is further evidenced by the high cathode efficiencies noted, which can only be explained by assuming reduction from a Ti^{2+} state.

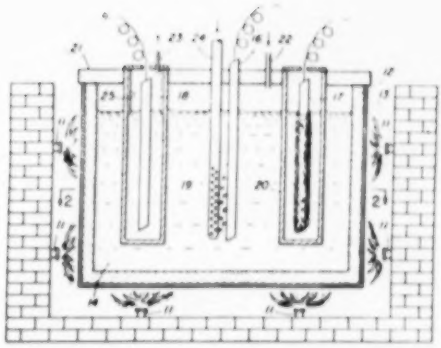
Soluble anode processes employing impure titanium as a feed material and using a well reduced bath give cathode current efficiencies of the order of 80-90 pct based on a 2 electron change. There is of course always some Ti^{3+} present.

Soluble anode processes are reported to be effective in separating the following impurities from titanium in chloride systems: oxygen, nitrogen, iron, and to a lesser extent, chromium. They have been reported not as effective for manganese or vanadium.[43] This is consistent with Emf values for fused chlorides recently published by Hamer[46] and Delimarskii.[47] The ΔE value from Ti^{2+} to Mn^{2+} and V^{2+} is <0.1 and 0.2V respectively and to Cr^{2+}, <0.5V.

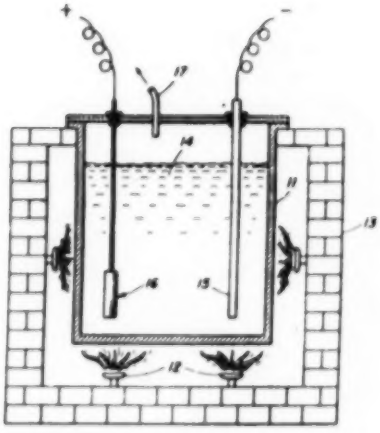
From recent data on refining processes for titanium,[36, 43] it is apparent that titanium species present in the melt is an important factor in the success of such a process, both in respect to total titanium present and to the oxidation state of the species. It appears that relatively large titanium content is required for effective operation with respect to



Titanium dioxide electrolysis cell: 10—crucible; 12—argon inlet; 13—gas outlet; 14—heating element; 16—graphite anode; 18—cathode; 20—graphite barrier; 21—TiO_2.

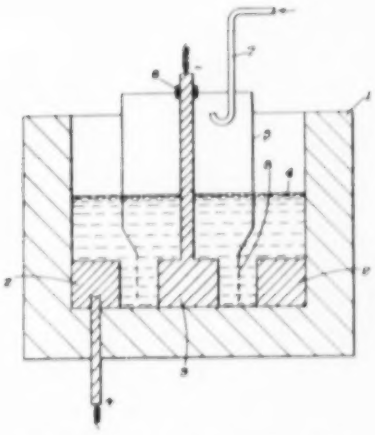


Titan Dual cathode cell: 11—gas heaters; 12—ceramic crucible; 13—metal sheath; 16—solubilization cathode; 17—deposition cathode; 18—graphite anode; 19—porous diaphragm; 20—porous diaphragm; 22—argon inlet; 23—gas outlet; 24—$TiCl_4$ inlet.

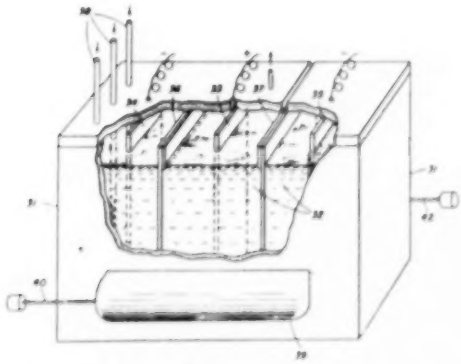


Titan soluble anode cell: 11—crucible; 12—gas burners; 13—refractory; 15—cathode; 16—impure Ti anode; 17—gas outlet.

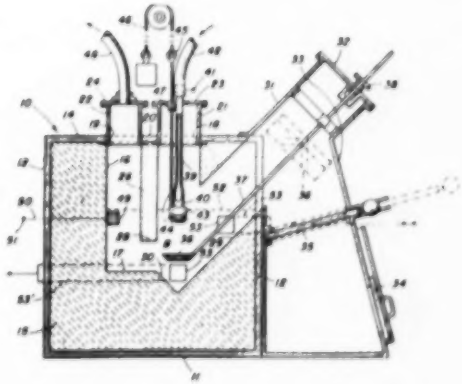
Cell Designs for Electrolytic Titanium Preparation



Shawinigan $TiCl_4$ cell: 1—refractory; 2—anode; 3—cathode; 5—metal bell; 6—metal screen; 7—$TiCl_4$ inlet.



Titan plate electrode cell: 31—crucible; 33—anode; 34—solubilization cathode; 35—deposition cathode; 36, 37—porous diaphragms; 38—$TiCl_4$ inlet; 40, 42—pumps; 39—salt transfer reservoir.



Titan cell with cathode removal device:[55] 15—refractory crucible; 39—cathode; 49—anode; 53—resistance heaters; 26—barrier; 44—cathode scraper; 36—retractable deposit removal pan; 35—slide gate; 34—deposit removal door.

cathode efficiency and particle size of powder deposited. It is also important that the melt be in as reduced a state as possible, i.e., that a maximum amount of Ti^{++} be present.

In the U. S. Bureau of Mines process, the lower chlorides are prepared external to the cell and diluted with NaCl to form a melt. The lower chlorides could also be produced in situ by chlorination of impure titanium with $TiCl_4$ or HCl. In both cases the final melt is high in Ti^{++}. In the latter case, $TiCl_4$ is more effective in yielding such a melt than is HCl. The reaction of $TiCl_4$ with titanium in a fused halide melt proceeds at essentially 100 pct efficiency at 800-900°C. Sodium, hydrogen or zinc reduction of $TiCl_4$ also are effective means of preparing $TiCl_3$-$TiCl_2$.

The soluble anode procedures appear to be an approach to a commercial process. Although only a minimum amount of information is available, they appear to be capable of a high degree of refining on a continuous basis.

Recovery of Electrolytic Titanium

Recovery of electrolytic titanium from the as-deposited salt-metal agglomerates presents certain problems in all three types of processes. Soluble salts, such as NaCl, $CaCl_2$, etc., are readily removed, but residual titanium salts and by-product oxides require special treatment.

Vacuum distillation procedures have been applied, but these are costly and of no value in the case of refractory contaminants. Aqueous recovery procedures offer the more practical approach, although more development is obviously needed in this area.

Deposits produced from oxides are generally contaminated by residual oxides and also by product oxides such as CaO. There is no satisfactory means of removing the titanium oxides, but oxalic or acetic acid washes are helpful. The liquid cathode approach offers some promise of alleviating the contamination problem in oxide processes.

In the case of halide and soluble anode procedures, the principal deposit contaminants are reduced titanium halides. These are objectionable in that they readily hydrolyze to give insoluble hydrates.

Washing in acid solutions is helpful in suppressing hydrolysis to a degree, but Ti^{++} species always react to some extent. In this respect fluoride deposits are more readily recovered than chloride, since divalent fluorides are present only in minute quantities. Washing in reasonably concentrated HCl can be carried out without appreciable loss of titanium, and it is beneficial in removal of hydration products produced in leaching.

Conclusions

On the basis of available information, it would appear that the process type with the greatest commercial potential would be a halide process. Both fluoride and chloride types offer advantages and also possess areas where further development could be done to assure them a more competitive position with present methods.

It is doubtful that oxide processes can be utilized as direct production methods, although they might be utilized to provide starting materials for a refining procedure.

The basic criteria governing the success of any such process will be quality of material and net cost of the product. On the basis of present costs, both K_2TiF_6 and $TiCl_4$ would not be too attractive as starting materials unless substantial reductions could be effected in their processing costs. However, in an efficient straightforward electrolytic process, it is possible that a sufficiently economical operation could be achieved so as to offset the materials cost.

A successful electrolytic procedure will likely be a continuous or semi-continuous one, since the equipment required will probably be somewhat unconventional (i.e., use inert atmospheres, facilities for high temperature handling of molten salts, etc.) and frequent shutdowns would be undesirable.

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Some Effects of Hot Strip Mill Rolling Temperatures on Properties of Low Carbon Sheet Coils

The phase changes occurring in low carbon steel during hot strip mill rolling are shown to be metallurgically significant when related to commonly used temperature control points, particularly finishing and coiling temperatures. In combination, these temperatures are shown to have an important influence on the level and uniformity of hardness, grain size, and carbide characteristics of the finished hot and cold rolled sheets.

by D. T. Goettge and E. L. Robinson

PRODUCTION of wide flat-rolled products ordinarily requires a number of operations in sequence to prepare the material for shipment to the customer. Most products are tailor-made for specific end uses, with each operation contributing certain properties to the finished material. Since the characteristics imparted to the semifinished product by a given step in processing carry through to the finished product in varying degrees, it is important that the intermediate stages of production of flat-rolled strip be carried out with the same care which characterizes the last or finishing operations. The step of hot strip mill rolling is common to the production of all of the various types of flat-rolled product; therefore, the hot strip rolling is an especially important point at which to recognize and control those variables which have an effect on the surface characteristics and metallurgical properties of the finished product and which influence the ease of conducting subsequent operations.

Orders entered at a producing mill usually show an end use or describe an article or part into which the ordered product is to be fabricated. Applying his experience as to the properties necessary in a finished sheet to suit the end use and to perform successfully in the fabrication involved, the metallurgist selects a steel of suitable composition and deoxidation practice, and slabs of appropriate dimensions are produced for rolling on the hot strip mill. At this stage of processing, the metallurgist faces the problem of controlling hot strip mill practice in the light of his diagnosis of the properties necessary to meet the end use, paying due attention to the accompanying problem of producing a strip which can meet processing requirements on subsequent units in the mill. It is the purpose of this paper to describe some of the factors which he must consider in solving these problems and to indicate some of the principles which guide him.

Equipment, Physical Requirements of the Strip, and Temperature Measurement

The metallurgist must, of course, be familiar with the physical layout of the mill, the temperature-measuring equipment available, and the physical requirements of the hot strip product before he can apply his metallurgical knowledge to the problem; hence, the first section will consist of a brief discussion of these matters.

The usual hot strip mill consists of reheating furnaces, five or six roughing stands including a scalebreaker, holding table, and second scalebreaker, six-stand finishing mill, runout table with spray cooling facilities, and coilers. A schematic diagram of a typical layout is shown in Fig. 1. Slab temperatures are primarily a function of heating time and furnace temperatures, while mill speeds, spray practice, drafting practice, available water pressure, temperature of the cooling water, cross sectional dimensions of the strip, coil size, and equipment limitations, either singly or in combination, determine what rolling temperatures are practical on a given hot strip mill unit. Thus, it is possible that a set of temperatures which can be utilized successfully on one mill cannot be used on another. However, adjustments in temperatures and rolling practice can usually be made to develop the desired metallurgical properties. In addition to the metallurgical properties developed through proper temperature control, the hot strip mill must also provide strip with certain physical attributes which may be summarized as follows:

Strip Cross Section—The strip contour should conform to a section which will give the best results in the cold reduction operation. This is generally recognized as a strip with 0.001 to 0.003 in. crown or shoulder-to-shoulder convexity depending on width, and freedom from concave, flat, or wedge-shaped cross sections which cause metal *buildup* in cold reduction. Excessive drop off in thickness at the edges can also be very detrimental in cold reducing to light gages.

Gage, Width, and Camber—All of these must be controlled. For example, rundown or increasing thickness from the front to the back of the coil results in nonuniformity in the thickness of hot-rolled sheet product and in added difficulty with gage and welds in cold reduction. Similarly, excessive width variation is the cause of guide trouble and excessive edge scrap at later stages of processing, while excessive camber is the source of a variety of processing troubles.

Type of Oxide—Product intended for pickling should have a predominance of the type of oxide most easily removable in sulfuric acid. It is generally recognized that this type is obtained by use of maximum table cooling water and cold coiling

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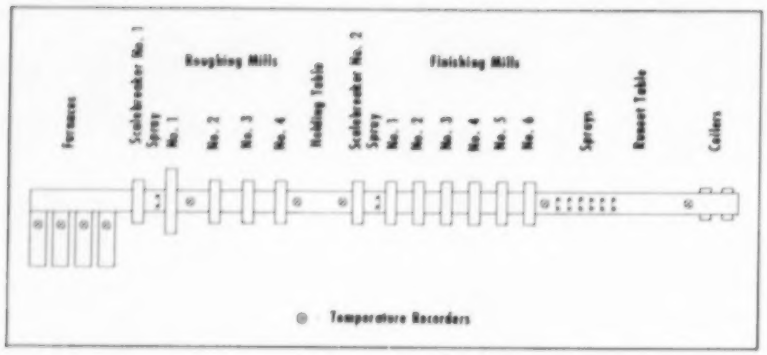

Fig. 1—Typical hot strip mill is shown diagrammatically. Location of the furnaces, scalebreakers, roughing mills, holding table, sprays, finishing mills, runout table, and coilers is shown.

temperatures. Where these conditions conflict with the temperatures required to develop specific metallurgical properties, a compromise pattern is usually established.

Surface and Edge Characteristics—Strip should be free of rolled-in scale, scale flashing, heat pattern, pits, and other surface defects which could affect the quality of the finished product. The condition of the edges is especially important, since cracked or slivered edges can result in strip breakage in cold reduction and in surface defects originating from scraps of edge metal breaking off from the strip.

Since the continuous hot rolling of strip from slabs is accompanied by phase changes in the product, it is logical that hot strip mill rolling temperatures are quite important in determining the properties which are developed in the hot-rolled coil and in the final product. For this reason, most modern mills have developed temperature practices for controlling grain size, carbide size and distribution, and hardness of the strip. The relationship of these phase changes to the ordinary temperature control points is depicted graphically in Fig. 2. The four major temperature control points may be summarized as follows: 1) initial slab temperature, 2) roughing temperature—recorded out of the last roughing mill, 3) finishing temperature—recorded out of the last finishing mill, and 4) coiling temperature—recorded just ahead of the first coiler a few seconds prior to coiling the strip.

The finishing and coiling temperature will be noted to have the most direct association with phase changes, and these temperatures are considered to be the most important from the standpoint of most metallurgical properties. Since these two temperatures and the cooling rate between the temperatures are very closely related, it is common practice to speak of finishing and coiling temperatures and their effect on properties in combination rather than singly.

It is obvious from the foregoing discussion that the temperatures at the hot strip mill must be measured as accurately as possible if the controls are to be effective. The fact that these temperatures are surface measurements taken by optical or radiation instruments makes them subject to errors introduced by interfering elements such as water, steam, oxide, and dust. This interference can usually be minimized by judicious use of air, steam, or water sprays to clear the strip areas where temperature observations are being made. Improvements in temperature measuring and recording instruments, together with constant checking and maintenance, have contributed to more accurate temperatures over the years. However, it should be recognized that the temperature varies to some extent throughout the length of a given slab or coil, and the actual finishing or coiling temperature is a range, simplified to a specific representative value, Fig. 3.

Hot-Rolled and Hot-Rolled Pickled Product

As has been pointed out, the hot strip mill metallurgist in dealing with a particular order is confronted with first of all a choice of an appropriate steel as regards composition, deoxidation, and steel finishing practice. His next task is to specify an attainable set of finishing and coiling temperatures for control of mechanical properties and grain size, according to his knowledge of the demands the customer will make of the finished product. For the purpose of this paper, it will be assumed that a rimmed steel of conventional composition (0.10 max C, 0.30 to 0.40 Mn) has been applied, and an attempt will be made to throw some light on his hot strip mill problem by rolling slabs into strip at various finishing and coiling temperatures and noting the effect of the various temperature combinations on the physical properties, ferrite grain characteristics, and carbide size and distribution of the hot-rolled product. For convenience, in referring to the various temperature conditions, the following will be considered: 1) Coils which have been finished at temperatures above 1600°F will be considered to have been finished *hot;* those finished below 1530°F will be regarded as finished *cold*. 2) Likewise, coiling temperatures in the range 1300° to 1350°F will be considered *hot*, and those below 1150°F will be classified as *cold*. Each combination of finishing and coiling temperatures will be represented by roughly 16 slabs so selected as to minimize the effect of steelmaking variables so that something of the uniformity can be observed as well as of the level of results to be expected from each temperature combination.

Table I. Ladle Analysis, Pct

	C	Mn	P	S	Si
Heat A	0.07	0.37	0.005	0.024	0.005
Heat B	0.07	0.38	0.009	0.026	0.008

The slabs selected for the comparisons were the product of two rimmed steel heats with the ladle analysis given in Table I. Top cut slabs from each ingot were diverted from the rollings in order to eliminate the effect of chemical segregation from the results as much as possible. This provided two 8000-lb slabs from each ingot and the tests in the hot-rolled condition were taken at the head and tail of each coil at the entry end of the continuous pickler after normal McKay processing and cropping of coil ends. The gage and size at this stage of processing

was 0.090 in., 48¾ in. x coil. The middle and bottom cuts were systematically shuffled prior to cold reduction and sheet annealing in order to minimize variations in the final properties resulting from the annealing treatment. The finished gage of 0.036 in. gave a 60 pct cold reduction. Tests on the final cold-rolled product were taken after temper rolling 1 pct at locations corresponding to the top and bottom of each original slab. With this background of processing and testing details, the results obtained can now be examined, starting with the hot-rolled product.

Beginning with the grain size problem, it can be stated, by examination of the results, that finishing temperature is the major factor in controlling grain size. The effect of finishing temperature is illustrated in Fig. 4. As can be seen, a finishing temperature above 1600°F, 1615° in this case, produced a relatively uniform grain structure of a size near eight on the standard ASTM grain size chart. The slightly coarser grains at the surface are believed to reflect the presence of the lower metalloid rim of the steel used rather than any rolling temperature effect. The lower finishing temperature, 1525°F in this case, is well below the temperature for the austenite-to-ferrite transformation, usually taken as about 1560°F under hot strip mill conditions, and it can be concluded that some hot work has been done on the strip while considerable ferrite was present, at least in the regions near the surface of the strip. The result, illustrated in Fig. 4, is a characteristic grain coarsening in these areas so that a fine grained core, coarse grained surface results. (It has also been established a number of times that gross recrystallization and a very coarse grain results when the low finishing is followed by the annealing effect accompanying a high coiling temperature.) It can be concluded immediately that for those applications where the uniform grain of normalized material is required, a high hot strip mill finishing temperature may allow the elimination of the normalizing treatment, since the high temperature structure approaches that of a normalized product. The carbide size is dependent primarily on coiling temperature, Fig. 5. Coarse carbides and pearlite result from the higher coiling temperature, 1335°F in this case, and are the result of several metallurgical phenomena. First, some austenite-to-pearlite transformation is undoubtedly taking place in the coil during a relatively slow cooling period through a temperature range near and below 1335°F, this

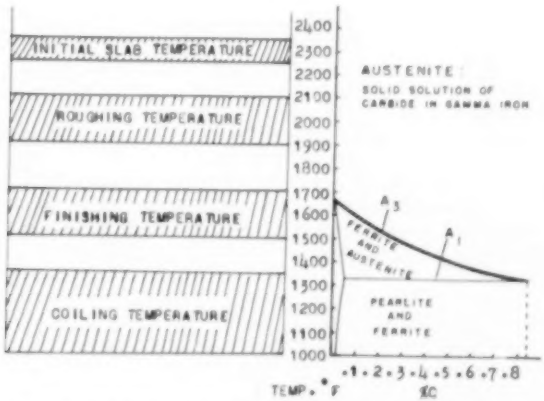

Fig. 2—Relation of hot strip mill temperature control points to the phase changes of product is shown.

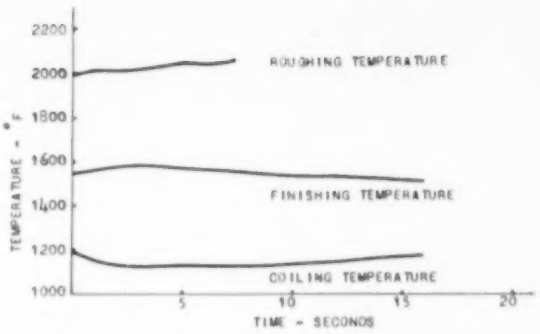

Fig. 3—Typical relationship of hot strip mill temperature control points is shown.

temperature being chosen in this case because it closely approximates the A_1 temperature. Secondly, considerable agglomeration of previously precipitated carbides, as carbides or in pearlite, takes place during this period. Finally, the carbides which precipitate from the ferrite as the carbon solubility in ferrite decreases with decreasing temperature below 1335°F, precipitate at the highest possible temperature and have an opportunity to grow in size. Conversely, the effect of a lower coiling temperature, 1115°F in this case, is to encourage formation of fine pearlite and to afford limited opportunity for agglomeration of the carbides.

Table II. Hardness of Hot-Rolled Sheets at Various Finishing and Coiling Temperatures

	Coil Hot		Coil Cold	
	Average, R_B	Range, R_B	Average, R_B	Range, R_B
Finish hot	55	53 to 58	58	55 to 59
Finish cold	42	37 to 56	54	45 to 66

The carbide effect occurring in the finished coil which has just been described is superimposed on the effect of finishing temperature, which primarily affects carbide distribution, the carbide stringers characteristic of low finishing temperature showing the effect of hot work on the dual austenite-ferrite structure, while the uniform distribution attending high finishing temperatures is an expected result of the presence of austenite alone until all mechanical work has been completed.

Examine, now, the mechanical properties which can be produced by regulating finishing and coiling temperatures and, as has been seen, grain structure and carbides. In considering the properties produced, only Rockwell hardness will be discussed; it is understood that yield point, ultimate strength, and general ductility level hold the usual relationship to the hardness, Table II. Examination of this table will show that finishing and coiling temperatures must be considered together in discussing the effect of hot strip mill practice on hardness. As can be seen, hot finishing produced a relatively high Rockwell level which is only mildly responsive to coiling temperature. The outstanding characteristic of the finished hot material is its uniformity, the hardness of the 16 slabs varying by only a few Rockwell points about the average for both coiling conditions. The cold-finished material is generally softer than hot-finished material, is prone to pronounced nonuniformity, as shown by the spread in Rockwell

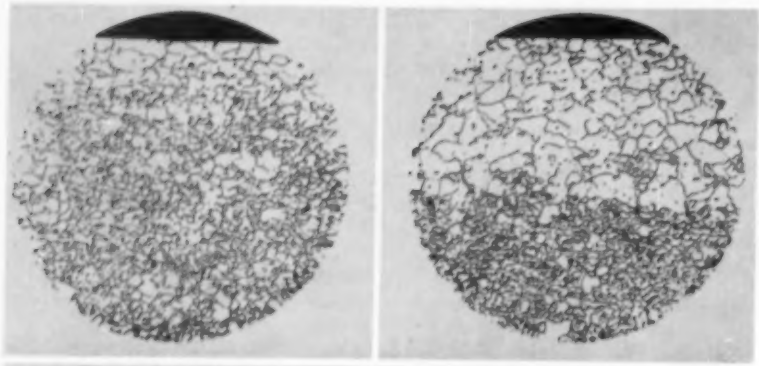

Fig. 4—Micrographs show effect on grain size of high and low finishing temperatures. LEFT: Finishing temperature, 1615°F; coiling temperature, 1340°F. RIGHT: Finishing temperature, 1525°F; coiling temperature, 1330°F. Nital etch. X100. Reduced approximately 20 pct for reproduction.

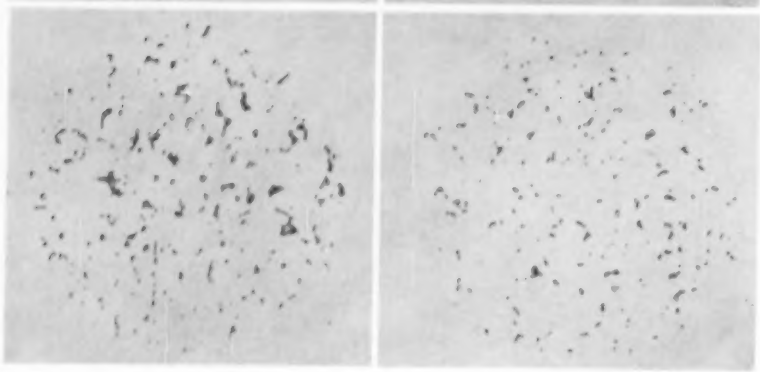

Fig. 5—Micrographs show effect on carbide size of high and low coiling temperatures with high finishing temperature. LEFT: Finishing temperature, 1620°F; coiling temperature, 1335°F. RIGHT: Finishing temperature, 1620°F; coiling temperature, 1130°F. Picral etch. X300. Reduced approximately 20 pct for reproduction.

values which exists for both coiling conditions, and most noticeable, is extremely responsive to coiling temperature with a difference of 12 points in Rockwell level attributable to coiling temperature alone. It is also noteworthy that the characteristic spread in hardness of cold-finished material is accentuated by a high coiling temperature.

The major tools of the hot strip mill metallurgist are now apparent. If he is processing an item requiring the drawability that goes with a normalized grain structure, such as an automotive cross member application, he must specify a high finishing temperature and attain a further minor regulation of his properties by manipulating coiling temperature. In so doing he can be reasonably certain that a product with high drawability is being manufactured and that uniform behavior in the customer's press can be expected. If a very low hardness value is required, as for example where the application involves a severe springback, the metallurgist may specify cold finishing and hot coiling, yielding some uniformity for low hardness. He may on occasion permit a low finishing temperature where a scale hazard exists, compromising with optimum uniformity, and he may specify a low coiling temperature in the interest of better pickling characteristics. In each case the metallurgist has been required to make decisions which in his judgment will result in a product having the best overall quality for the particular usage to which it will be applied.

Cold-Reduced and Annealed Product

Hot-rolled coils are pickled, cold-reduced, annealed, and temper-rolled to make the standard cold-rolled sheet. With such a succession of treatments, all affecting the properties of the finished product, it is not surprising that, for a period of several years in the early history of the wide strip mills, the effect of hot strip mill rolling practice on the properties of finished cold-rolled sheets was not well understood. It is now generally agreed that the hot strip mill practice is very important in this regard, and it is the purpose of this portion of the paper to outline some of the ways in which hot strip mill practice influences the properties of the cold-rolled and annealed product.

Table III. Grain Size of Cold-Reduced and Annealed Sheets as Influenced by Hot Strip Mill Finishing and Coiling Temperatures

	Coil Hot		Coil Cold	
	Average, ASTM No.	Range, ASTM No.	Average, ASTM No.	Range, ASTM No.
Finish hot	6.8	6.5 to 7.5	7.6	7.0 to 8.5
Finish cold	6.9	6.5 to 9.0	7.5	6.5 to 8.5

The material used again consisted of roughly 16 slabs for each point of the four areas of hot and cold-finishing and hot and cold-coiling. *Hot* and *cold* are defined as the temperature ranges indicated in the previous section of this paper. The slabs were selected systematically in an effort to exclude steelmaking and ingot characteristics from the study and the finished product was studied from the points of view of grain size, carbide size and distribution, and mechanical properties as represented by hardness.

The grain size effect is shown in Table III, a tabulation of ferrite grain sizes in standard ASTM grain-size numbers. The results differ substantially from the relationships previously shown for hot-rolled product. For example, the average grain size of cold-rolled product is much more responsive to coiling temperature than it is to finishing temperature, a relationship similar to the carbide relationship found in hot-rolled product but the reverse of the grain-size relationship for hot-rolled product. On

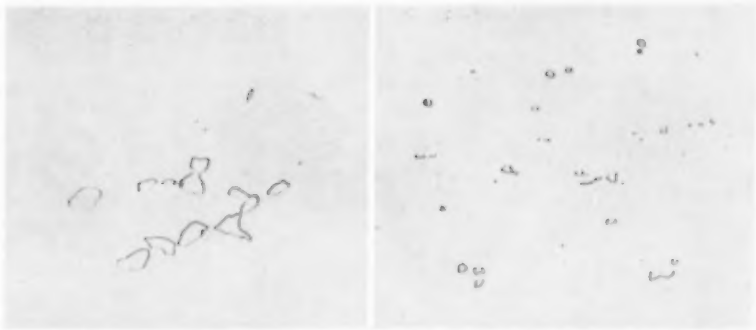

Fig. 6—Effect of high and low coiling temperatures with high finishing temperature on carbide size of cold-rolled product after subcritical annealing. LEFT: Finishing temperature, 1625°F; coiling temperature, 1335°F. RIGHT: Finishing temperature, 1610°F; coiling temperature, 1145°F. Picral etch. X1000. Reduced approximately 20 pct for reproduction.

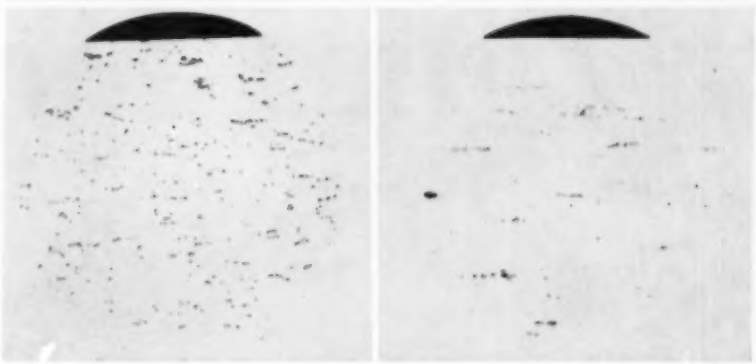

Fig. 7—Effect of high and low finishing temperatures with high coiling temperature on carbide distribution of cold-rolled product after subcritical annealing. LEFT: Finishing temperature, 1625°F; coiling temperature, 1335°F. RIGHT: Finishing temperature, 1530°F; coiling temperature, 1335°F. Picral etch. X100. Reduced approximately 20 pct for reproduction.

the other hand, the spread in grain size about the average for each group is minimized by high finishing temperatures. The conclusion then is that grain-size level can be controlled through control of coiling temperatures and that uniformity is favored by high finishing temperatures. The explanation for these relationships is not entirely clear, but it appears quite probable that carbide size, dependent primarily on coiling temperature, is influencing the recrystallization habits of ferrite during annealing following cold reduction. It is not so clear that the effect of higher finishing temperatures in bringing about a uniform carbide distribution is the basic phenomenon underlying the tendency for the grain size of cold-reduced and annealed sheets to be more uniform with higher finishing temperatures.

As for putting this knowledge to work, the production of black plate for deep cylindrical draws is an example of an end use application directly associated with hot strip mill temperatures. The best results are obtained with a combination of high finishing and coiling temperatures.

The effect of hot strip mill practice on carbide size and distribution in cold-reduced and annealed sheets is quite similar to its effect on carbide habits in hot-rolled sheets, with coiling temperature influencing carbide size and finishing temperature affecting primarily carbide distribution. Fig. 6 illustrates the effect of coiling temperature on carbide size in the finished cold-rolled sheets, showing coarse carbides with high coiling temperatures and fine carbides for low coiling temperatures. The explanation is the same as was given in the section under hot-rolled product, with the added comment that the subcritical anneal following cold reduction has not changed the relationship although it has undoubtedly brought about some growth and rounding of the carbides. In sheets of the thickness studied, 0.036 in., there is no evidence of carbide fragmentation during cold reduction from the usual starting gages. Carbide distribution (as opposed to carbide size) in cold-rolled sheets reflects the finishing temperatures on the hot strip mill, Fig. 7. As can be seen, the stringer configuration of the carbides which is associated with low finishing temperatures persists in the cold-reduced and annealed sheets, while the even distribution of carbides which comes with high finishing temperatures also persists through cold reduction and annealing. It should be realized that carbide distribution is a matter of considerable importance when drawing quality sheets for deep stampings are being produced. It is also a factor in the control of hydrogen embrittlement of cold-rolled product when acid pickling and is used to advantage in the manufacture of tin plate to develop specific properties.

Table IV. Hardness of Cold-Reduced and Annealed Sheets as Influenced by Hot Strip Mill Finishing and Coiling Temperatures

	Coil Hot		Coil Cold	
	Average, Rb	Range, Rb	Average, Rb	Range, Rb
Finish hot	43.5	41 to 46	46.5	42 to 48
Finish cold	42.7	38 to 50	45.3	38 to 51

The mechanical properties of cold-reduced and annealed sheets, as reflected in Rockwell hardness values, are shown in Table IV. The observations to be made are similar to those concerning grain size, with coiling temperature exerting the greatest influence on hardness level, the higher coiling temperatures producing low Rockwell values, and vice versa. Likewise, a high finishing temperature is conducive to uniformity, as was the case with grain size.

The production of killed nonaging sheets for deep drawing applications and the role of hot strip mill

practice in control of killed steel properties are subjects which, for an approach to adequate coverage, would require as much space as has already been used. It is sufficient merely to indicate that the relationships which have already been described exist only in part in the aluminum killed steels, and that a second and quite different set of relationships based on the solubility-temperature habits of aluminum nitride in an iron matrix governs the choice of hot strip mill practice.

Conclusion

It has been shown that hot strip mill practice is indeed an important factor in its influence on the characteristics of both hot-rolled and cold-reduced and annealed sheets. In all cases a finishing temperature above the upper transformation temperature is important in achieving uniformity, whether grain size, carbides, or mechanical properties are being discussed. Coiling temperatures play an important role in determining all of the described items, with a number of interesting variations between their effect on hot-rolled and on cold-reduced product. In actual practice, finishing and coiling temperatures between the extremes cited are frequently specified in the interest of scale control or other practical considerations, but it is a firm principle that, in every instance, whether cold or hot-rolled sheets are being produced, a discerning choice of hot mill practices is an important duty of the metallurgist.

Discussion of this paper sent (2 copies) to AIME by Nov. 1, 1956, will appear in *AIME Transactions* Vol. 209, 1957, and in JOURNAL OF METALS, October 1957.

Factors Affecting Coiling Temperatures in the Hot Strip Mill

An investigation was carried out to determine the variables affecting the coiling temperature of strip in the 56-in. hot strip mill. After statistical treatment, equations have been derived to determine the temperature drop between mill and coiler for both full spray (top and bottom) and top spray only practices. The individual influences of each variable in the temperature drop were derived for both practices. Limitations of the present practices were shown and adjustments in cooling for increased rolling speeds were suggested. A method of estimating the required number of sprays, their spacing, pressure, etc., to achieve a desired temperature drop is developed.

by J. G. Sibakin and G. M. Ikeda

MAXIMUM finishing temperature of a hot strip mill is mainly determined by the rolling speed, gage, and the temperature of the bar entering the mill. This finishing temperature may be limited by the maximum admissible coiling temperature in cases when either the cooling facilities on the runout table are not adequate or, more generally, when the cooling capacity of the runout table and the production capacity of the continuous mill are out of balance.

Some time ago, the balance between these two factors was not considered detrimental to production. Recently, a new drafting schedule was introduced which increased the production rate, the entry temperature, and hence the finishing temperature. Thus, the restriction of the maximum specified coiling temperature became an important factor for many of the thicker gages. An investigation of the factors determining the coiling temperature may be, therefore, of considerable importance.

A brief description of the available equipment will be a useful illustration of the techniques and methods used for this investigation.

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The 56 in. continuous hot strip mill is composed of six 4-high stands each containing two 22½-in. diam work rolls and two 45-in. diam back-up rolls. Either of the last two stands can be used as the finishing stand. Which stand is used is dependent on the total percentage of reduction to be taken over the whole train, i.e., on the entry thickness and the final gage.

The characteristics of these last two stands are given in Table I.

Table I. Operational Characteristics of Nos. 5 and 6 Stands

	No. 5	No. 6
Maximum voltage	600	600
Power, hp	3000	3000
I normal, kiloamp	4.35	3.73
Rpm of motor and rolls	150 to 300	175 to 350
Speed of strip, fpm	883.6 to 1767.2	1031 to 2062

The speed of the finishing stand varies with the amount of reduction, strip width, and the exit gage. Finishing speeds in general use for varying gages are shown in Table II.

These speeds may change with an increase or decrease in the entry temperature of the strip. Even though the maximum speed of No. 6 stand is shown as 2062 fpm, this has so far never been attained,

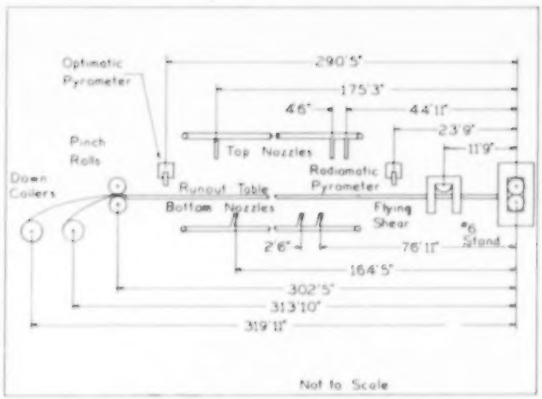

Fig. 1—Diagrammatic elevation of the strip mill runout table showing the important features of the table and the distances from the last stand of the continuous train.

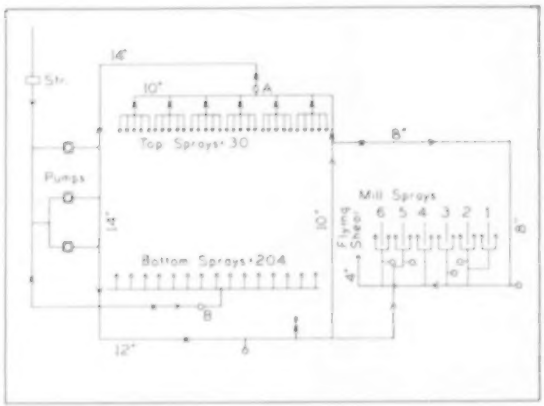

Fig. 2—Diagrammatic layout of the spraying system for the strip mill rolls and the runout table, showing the size and relationship of the main and secondary piping. The top table sprays are in separately controlled groups of five. The bottom sprays are in 34 rows of six nozzles and they are operated together as a single unit.

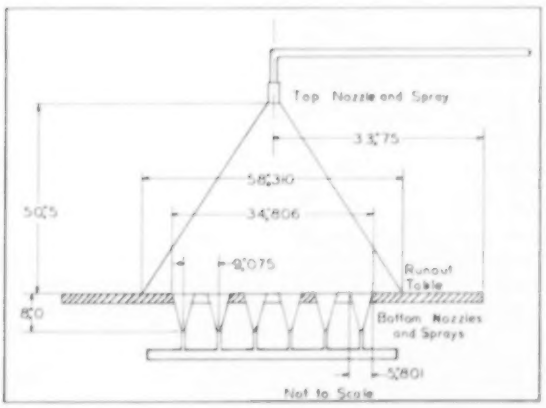

Fig. 3—Cross section, end view, of the hot strip mill runout table showing the distances from the table to both the top and bottom spray nozzles. The maximum width coverage for both top and bottom sprays is also shown at 60 psig.

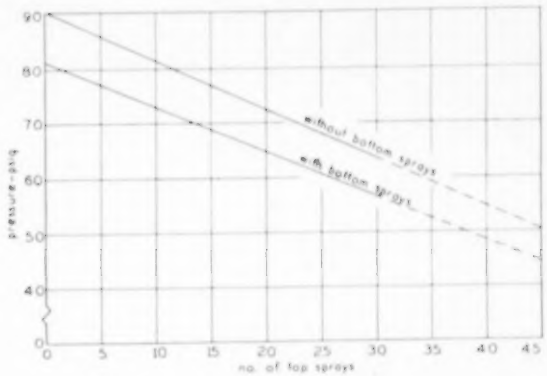

Fig. 4—As the number of top sprays used increased, the pressure decreased linearly. This function also held true for bottom and top sprays. The initial pressure in this case was lower and the two lines converged gradually with increasing number of top sprays.

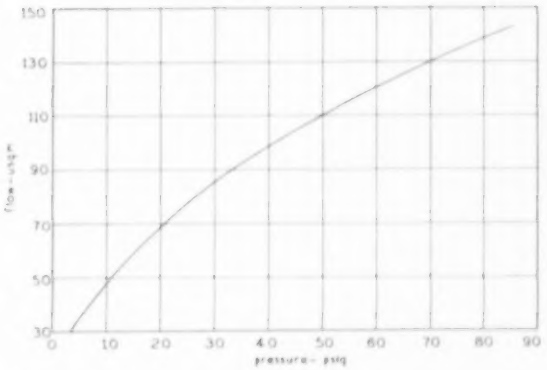

Fig. 5—Flow of water from the top nozzle increases curvilinearly with the pressure. This graph was plotted from information obtained in the manufacturer's catalog.

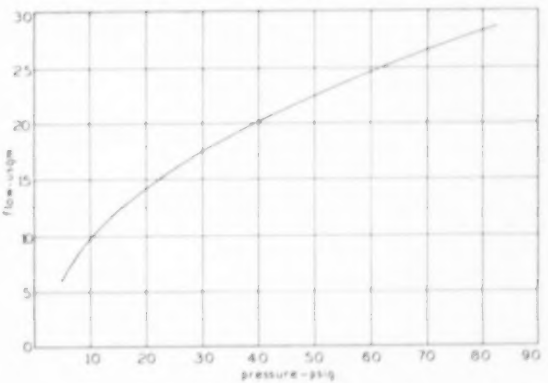

Fig. 6—Graph for the flow from a bottom nozzle was plotted from information obtained from the manufacturer's catalog. These values, which are shown here and in Fig. 5 above, were converted into total flow, see Fig. 7 on opposite page, and were extrapolated 15 sprays beyond the present system to cover the requirements of any expansion.

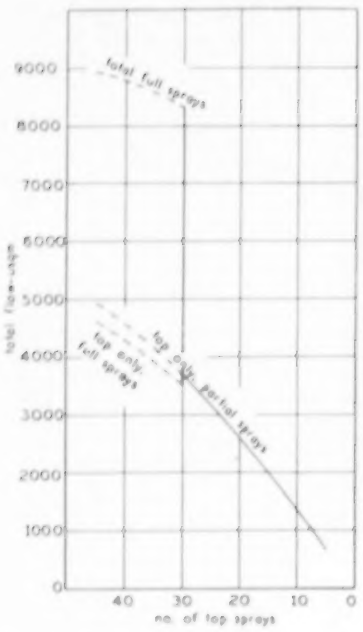

Fig. 7—Total flow of water through the nozzles was found to increase curvilinearly with increasing number of top sprays. The spraying practice in use has a maximum of 30 top nozzles and values were extrapolated for an additional 15 sprays. In this extrapolation the flow values differed for the three spray practices: partial top sprays, total top sprays, and total top and bottom sprays.

since at that speed the head end of the coil would become airborne on the runout table and considerable trouble would be experienced at the coilers with the present setup.

It should be realized that the increase in the production rate of the roughing mill resulted in increased finishing temperatures even with the speeds indicated in Table II. An increase in the production rate in the continuous mill, which is within the range of possibility, will be conducive to a further increase in finishing temperature.

The rolls are cooled by four sprays, one on the entrance and exit side of each work roll.The water is sprayed through a bank of 64 nozzles on each side of the rolls.

Runout Table and Spray System

The flying shear, Fig. 1, follows the last stand at a distance of 11.75 ft. Just past the shear, a radiamatic pyrometer is installed to record the finishing temperature. The runout table is 5.63 ft wide and 313.8 ft in length from No. 6 stand to the first down coiler. An optimatic pyrometer for reading the coiling temperatures is situated 290.5 ft from No. 6 stand.

The cooling water is supplied through three parallel Pomona turbine pumps, Fig. 2, into a common 14-in. diam pipe which feeds the top, bottom, and mill sprays. The piping is arranged to ensure minimum pressure differential over the entire system.

The 30 top spray nozzles start 44.9 ft from No. 6 stand with an interval of 4.56 ft between sprays, giving a total sprayed length of 132.3 ft. This length covers the distance between the center line of No. 1 spray and the center line of No. 30 spray and does not take into account the length covered by the first and last sprays outside their center lines. They are supplied from the common 14-in. pipe through a globe valve into a 10-in. diam pipe with seven outlets. Six of these are fitted with air valves which control five 2½ H490 S.Q. (manufacturer's specification No.) full jet nozzles. These nozzles are placed 4.2 ft above the runout table, Fig. 3, which gives a width coverage of 4.86 ft at 60 psig.

Table II. Approximate Finishing Speeds for Strip of Varying Gages

Gage	Thickness, In.	Approximate Fpm for 36 In. Width
—	0.312	1000
—	0.287	1100
—	0.250	1100
3	0.229	1100
4	0.224	1100
5	0.209	1200
6	0.194	1200
7	0.179	1200
8	0.164	1350
9	0.149	1400
10	0.134	1450
11	0.119	1450
12	0.104	1500
13	0.090	1500
14	0.074	1500
15	0.067	1500
16	0.059	1550
17	0.054	1550
18	0.047	1550

The 34 sets of bottom nozzles, which are in groups of six, are placed 8 in. below the runout table. The breadth coverage of the six nozzles is 2.9 ft. The first set of nozzles start 76.9 ft from No. 6 stand and cover a total length of 86.75 ft. These ½μ 40200 vee jet nozzles are supplied from a common 14-in. pipe through a globe valve into a single pipe feeding each set of nozzles.

Spray Practice

The present spraying practice in use on the runout table may be separated into two types, that using only top sprays and that using 30 top and all-bottom sprays. The number of top sprays required, which may only be varied in groups of five nozzles, is determined by the coiling temperature range specified for gage and grade. The use of 30 top and all-bottom sprays, which is called the full spray practice, occurs when maximum cooling is required.

It can safely be assumed that the temperature loss on the runout table will be dependent upon the initial temperature of the strip (finishing temperature), the time during which the strip is in contact with the cooling water, the gage, and possibly the pressure of the cooling water. The time in which the strip is in contact with the cooling water is determined by the length of the sprayed area and the speed of the moving strip. The amount of cooling on the runout table may also be affected by the type of nozzles used although, it is felt, this factor is of only

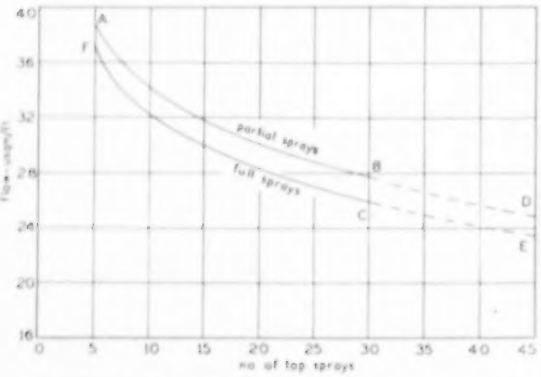

Fig. 8—Flow per foot of sprayed area decreases with an increase in the number of top sprays used. *AB* shows the values for top sprays only and *FC* shows the corresponding values with full bottom sprays (the use of full bottom and partial top sprays is not standard practice). *BD* and *CE* are extrapolations for an additional 15 top nozzles.

secondary importance. Anyway, as nozzles of different types were not available during this investigation, this influence was not established quantitatively. It is probable that, should such an influence exist, it will be largely covered by the influence of the spray pressure.

The width of the strip should not be of importance for the cooling rate, provided a sufficient amount of water is available and the cooling is mainly governed by the contact between strip and water. Further calculations have fully substantiated this assumption and, therefore, the width was eliminated as a factor affecting the cooling on the runout table. In order to introduce into the problem the influence of the amount of water on the cooling rate, the flow per foot of sprayed area in gpm (U. S. gallons) per foot was also used as a variable. This flow will be a function of the spray pressure. The gage was obtained from production schedules for strip rolling. Variations between ordered gage and as-rolled gage were not considered great enough to warrant the measurements of thickness on each coil at the coilers. The finishing temperature was taken as an average of head and tail readings. The speed of the last stand and the distance between the first and last spray in use determined the cooling time which was expressed in minutes under the sprays.

This separation of the two spraying practices simplified the problem considerably because two parallel studies could be carried out and, by a simple subtraction of the results, any combination of sprays would be available. This makes it possible to establish, among other factors, the individual influence of the top and bottom sprays.

Investigational Procedure

The only variable that could not be obtained directly was the flow per foot of sprayed area. These values were established from pressure readings at the top spray header. Only the top spray pressure was used for mathematical considerations because a common measurement was required for both problems.

Pressure readings were taken at the inlet and outlet of each pump and at points marked *A* and *B* in Fig. 2. Although slight variations in pressure were recorded, the discrepancy was considered within the measurable limits.

In order to obtain the pressure readings at full flow for varying number of top sprays, the values recorded were averaged and plotted graphically, Fig. 4. The resulting relationship was found to be linear. When the bottom sprays were introduced, the relationship was again linear but showed a convergence toward the line for top sprays as the number of sprays in use increased. Flow values per nozzle were obtained from graphs supplied by the nozzle manufacturer and are shown in Figs. 5 and 6. These values were converted into total flow, Fig. 7, and were extrapolated 15 sprays beyond the present system to cover the requirements of any expansion. The total flow through the top nozzles were then recalculated into flow per foot, Fig. 8.

The line *ABC* in Fig. 8 indicates the flow through the top nozzles for the present practice. The line *FC* which indicates the flow through the top sprays with the bottom sprays on is not used in practice but shows the decrease in flow caused by the bottom sprays. *BD* and *CE* are extrapolations of these two lines.

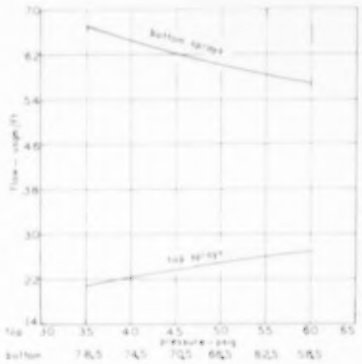

Fig. 9—Any change in the top spray pressure will induce a change in the bottom spray pressure assuming a constant flow from the pumps. This graph shows this effect on the flow per foot of sprayed area for both the top and bottom sprays operating simultaneously.

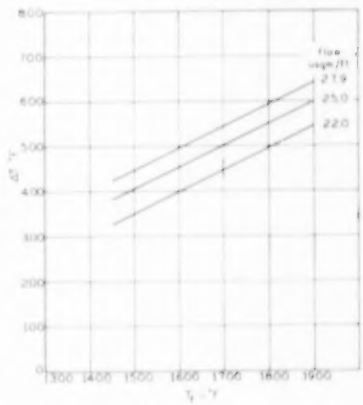

Fig. 10—Influence of the finishing temperature on ΔT for 0.070-in. strip with a contact time of 0.080 min is shown at various flows per foot values for full sprays.

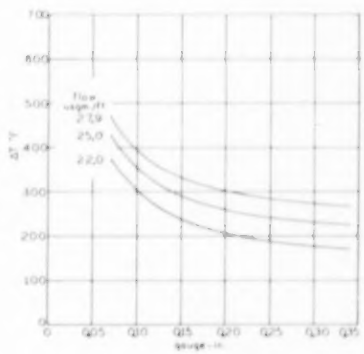

Fig. 11—With a constant spray time of 0.080 min and a constant T_f of 1550°F, the ΔT is a curvilinear function of gage. The influence of the flow per foot for full sprays is shown to produce a family of lines.

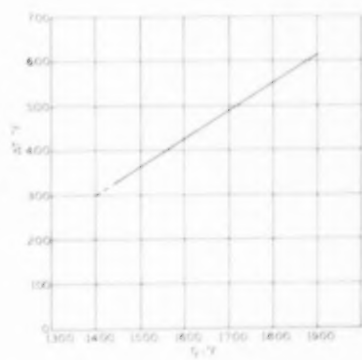

Fig. 12—Rise in ΔT produced by a rise in T_f is illustrated. To obtain this graph, constant values were used for spray time of 0.080 min, gage of 0.080 in., and flow of 28.1 gpm per ft.

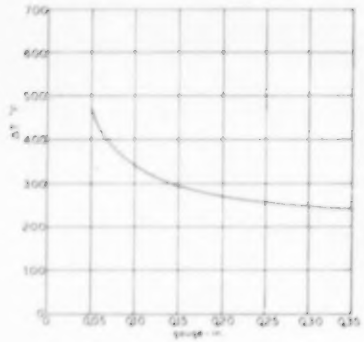

Fig. 13—Similar to Fig. 11, with a constant spray time of 0.080 min and a constant T_f of 1550°F. The slight change in the line is caused by the constant flow of 28.1 gpm per ft and the absence of bottom sprays.

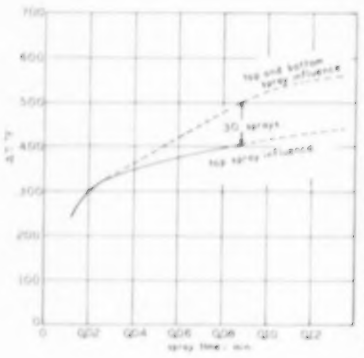

Fig. 14—By plotting the ΔT vs spray time for top and bottom and only top sprays, two curves will result. The interval between these two lines, then, establishes the influence of the bottom sprays. The slight change in the line is caused by the constant flow of 28.1 gpm per ft and the absence of bottom sprays. Table speed, 1500 fpm; gage, 0.080 in.

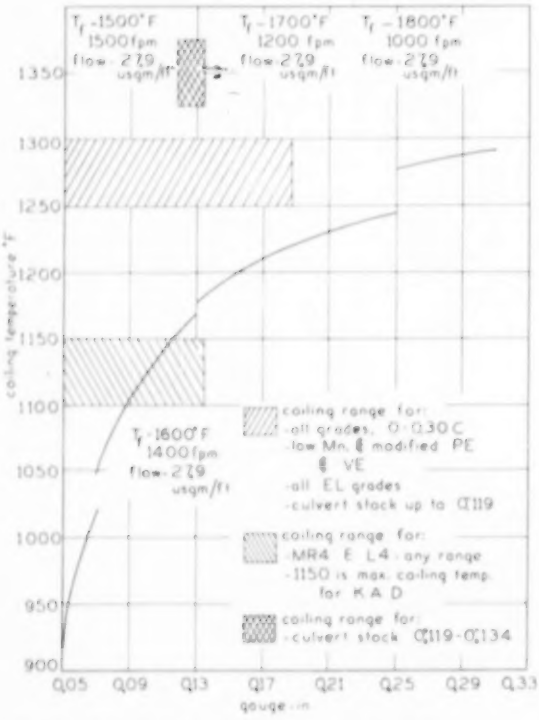

Fig. 15—Present practice in the hot strip mill. Coiling temperature is plotted vs gage. Coiling temperatures used for various grades and gages with their corresponding T_f and table speed are shown.

In order to establish the influence of the flow per foot with full top and bottom sprays, the top pressure was reduced by means of a globe valve between the common 14-in. diam pipe and the spray header. The results are shown in Fig. 9. Here the pressure at the top spray header and the equivalent bottom spray pressure is plotted against the flow per foot. In the subsequent study only the top spray flow was used, i.e., the variation in bottom spray flow was considered as a factor which could be neglected as far as the coiling temperature was concerned. Further calculation substantiated this assumption to a considerable extent.

Interpretation of Collected Data

When all the data for the investigation were collected, they were separated as previously mentioned and prepared for mathematical treatment.

The variables obtained for this study were not considered to be directly related to the coiling temperature, but to the difference between the finishing and coiling temperature denoted by ΔT.

The influence of the other variables on the ΔT was then considered. The finishing temperature was introduced as a linear function, since, all other variables being constant, any increase in the finishing temperature would probably result in a proportional increase in ΔT. The gage was taken as a hyperbolic function to explain the decreasing heat losses with thicker strip. This simple function was considered sufficiently accurate for this purpose, close enough to a more complex exponential function as in a more fundamental treatment. The time under the sprays was also introduced as a hyperbolic function to account for the decreasing heat flow, from the strip to the water, with decreasing instantaneous strip temperatures. The flow of water per foot was taken as a parabolic function, since its exact influence on ΔT was unknown. A curvilinear function was thought necessary, since the pressure, although it may increase the heat transfer, must gradually come to a maximum value due to the limiting influence caused by the rate of water removal from the strip. With these variables, a statistical correlation on 335 samples with a full spray practice was made. These samples included a pressure variance from full flow (60 psig) to 35 psig top spray pressure. The resulting equation is

$$y = -1034 + 0.4865x_1 + 18.05x_2 - 28.77x_3 + 48.52x_4 - 0.645x_5 \qquad [1]$$
$$n = 335$$

where $y = \Delta T$ with a variance in sample of 275 to 655; x_1 is the finishing temperature, variance of 1465° to 1890°F; x_2 is the reciprocal of gage, variance of 0.070 to 0.312 in.; x_3 is the reciprocal of time under spray, variance of 0.080 to 0.1477 min; x_4 is the flow per foot, variance of 22.2 to 28.6 gpm per ft; and x_5 is flow per foot squared.

The percentage of variance in ΔT explained by the independent variables used for the foregoing correlation was found to be 51.3 pct.

The width of the strip was omitted as a variable, since its influence was found to be negligible in a previous correlation.

The individual influences of the variables on ΔT are presented in Figs. 10 and 11. In Fig. 10, the finishing temperature, at constant gage and time and at three different flows, is shown. The slope of the

line for all curves is 0.49; therefore, for every 100°F increase in finishing temperature, there will be a 49°F increase in ΔT. For example, at 1500°F and 27.9 gpm per ft, ΔT is 447° and at 1600°F with the same flow, the ΔT is 496°F. The flow per foot characteristic may be seen from the diminishing differences in ΔT toward greater flow. As the heat is removed mainly by the water being in contact with the strip, there will be a value of the flow on the sprayed area beyond which a further increase in the amount of water will not result in additional cooling. This means that the strip may be flooded. The decreasing influence of the flow per foot on the ΔT supports this suggestion. The numerical value obtained for such a maximum flow (about 37.5 gpm per ft) may not be an accurate figure, since it is outside the range of figures which were available during this investigation. This, naturally, does not change anything in the established trend. In Fig. 11, the influence of gage on ΔT is plotted at constant finishing temperature and time under the sprays. These curves show a greater drop in temperature for lighter than for heavier gages.

The data for top sprays alone were treated in a similar way and the resulting equation is

$$y = -460 + 0.628x_1 + 13.52x_2 - 0.70x_3 - 11.25x_4 + 0.0177x_5 \qquad [2]$$
$$n = 259$$

where $y = \Delta T$, with a variance in sample of 215 to 485; x_1 is the finishing temperature, variance of 1445° to 1680°F; x_2 is the reciprocal of gage, variance of 0.059 to 0.187 in.; x_3 is the reciprocal of time under spray, variance of 0.0110 to 0.0915 min; x_4 is the flow per foot, variance of 28.1 to 39.2 gpm per ft; and x_5 is the flow per foot squared.

The total variance in ΔT explained in this correlation was found to be 60.0 pct. The influence of the finishing temperature on the ΔT is shown in Fig. 12. The different position and slope of the line reflect the absence of the bottom sprays in this case. The influence of gage, Fig. 13, is practically identical to the full spray equation but shifted to lower ΔT values. This is also the result of the absence of bottom sprays. As the gage increases, it is noticeable that the resultant ΔT approaches more and more to the corresponding values obtained from Eq. 1 for full sprays. This indicates the greater importance of the top sprays on the cooling of the strip, since they ensure a greater time of contact between the strip and the water. It is not possible to determine the individual influence of the time and the rate of flow from Eq. 2, since there is a definite relationship between the increase in pressure and an accompanying decrease in the number of sprays which imposes restrictions on the solution.

Discussion

The interpretation of these two equations to predict different lengths of sprayed area now became possible. The only restriction imposed on any solution according to Eq. 2 is the fact that for each number of top sprays there is a corresponding pressure, as shown by the line *ABD* in Fig. 8. The full spray equation, Eq. 1, on the other hand, may be used for any flow.

In order to obtain the influence of the bottom sprays on any length of sprayed area, the effect of the time under the sprays on ΔT for top sprays and bottom sprays may be drawn, as shown in Fig. 14. The values for both curves have been calculated

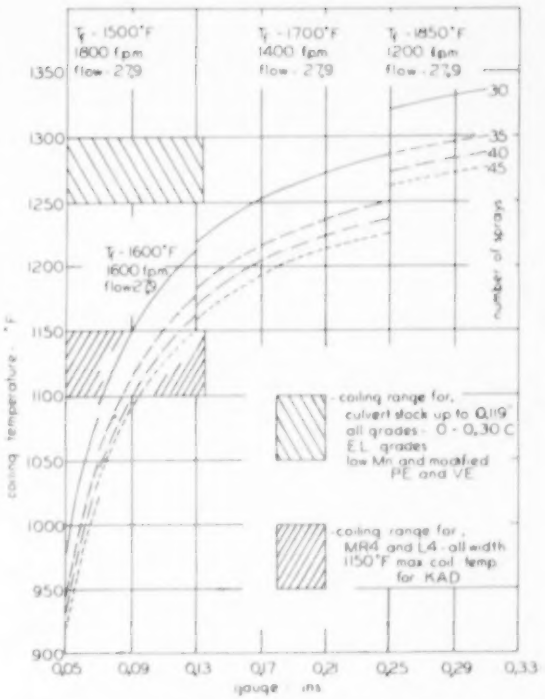

Fig. 16—Similar to Fig. 15 with coiling temperature plotted vs gage with a constant flow. The T_f and table speed have been varied to illustrate their effect on coiling temperature. Values for 35 to 45 top sprays have been extrapolated and included in the graph.

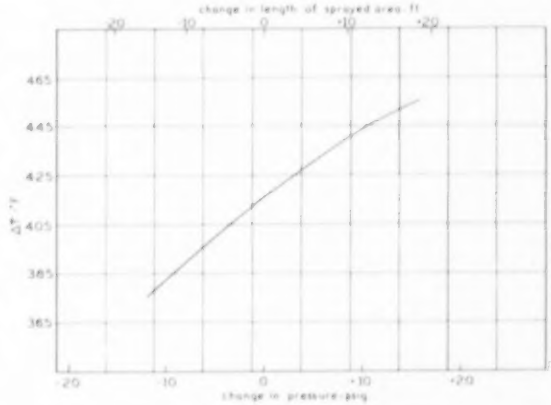

Fig. 17—This curve is a comparison of the increase in ΔT by increasing either the length of the sprayed area or the pressure. Variables held constant throughout were: T_f, 1600°F; gage, 0.134 in.; finishing speed, 1450 fpm; and flow, 27.9 gpm per ft. To determine the effect of the length of the sprayed area, the pressure was held at 62.5 lb above the curve, while the area was held at 132.3 ft (distance covered while using 30 top sprays) to find the influence of pressure (below curve).

with constant gage, finishing temperature, and speed of last stand. Since the length of the sprayed area was divided by the speed of the last stand to obtain the minutes under the sprays, the length of any sprayed area may then be obtained by using a constant speed and specifying a definite time under the

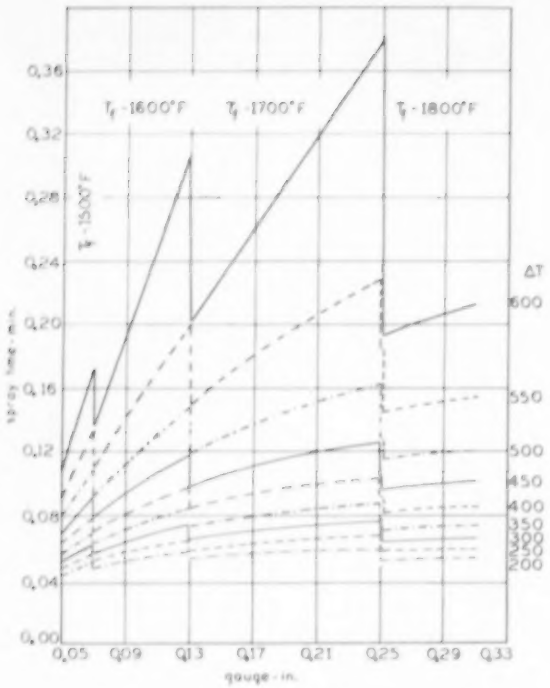

Fig. 18—Nomogram shows the spray time required to obtain any drop in temperature with a given gage and finishing temperature. The flow was constant at 25 gpm per ft. Various finishing temperatures may be used by the following correction: 100° change in T_f, 50° change in ΔT.

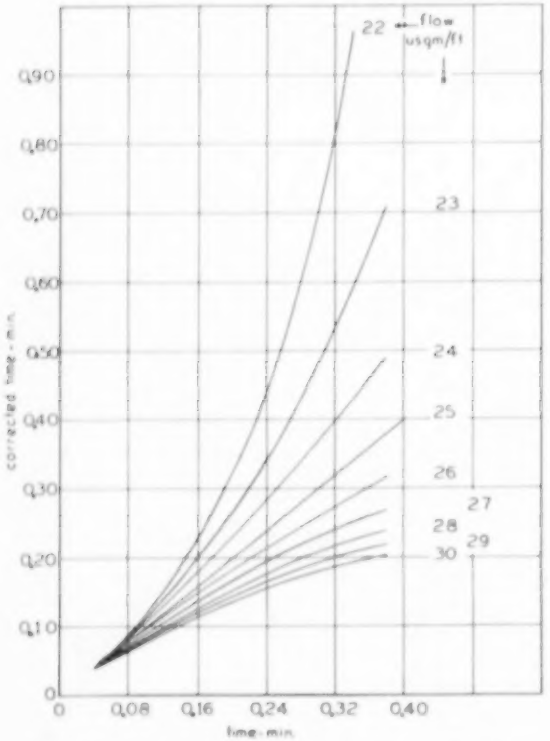

Fig. 19—Spray times determined from Fig. 18 are valid only for a flow of 25 gpm per ft. The graph corrects these times for varying flow.

sprays. Once the length of the sprayed area is established, the corresponding pressure can also be determined according to the relationship given by the line *ABD* in Fig. 8 for top sprays.

The relative positions of the two lines in Fig. 14 establish the influence of the bottom sprays on ΔT. The point where both lines coincide corresponds to the beginning of the area covered by bottom sprays. The point marked *30 sprays* on the graph corresponds to the full length of the present top sprays with all of the bottom sprays turned either on or off. The part of the graph to the left of this point corresponds to a proportional decrease in area covered by the bottom sprays until they are completely taken off when both lines coincide. The part of the graph to the right of the point marked *30 sprays* corresponds to a proportional increase of the areas sprayed by both top and bottom sprays. The relative position of both lines indicates that, when all the bottom sprays are on, the ΔT is increased by about 100° in spite of the much greater flow through the bottom nozzles (57.1 gpm per ft) as compared to the flow through the top nozzles (27.2 gpm per ft). A further, even considerable, lengthening of the area covered by bottom sprays results in quite a small increase in ΔT, as compared with the cooling that could be obtained by increasing the top sprayed area only. Similar results were obtained for all gages. It seems, therefore, that cooling is achieved mainly by the water being in contact with the strip and not by the impact of the stream.

This principle was applied to the present practice at The Steel Co. of Canada and to a proposed new practice using a considerably increased rolling speed. Fig. 15 shows graphically the present practice on the 56 in. mill with coiling temperature plotted against gage, using corresponding finishing temperatures and speeds. These curves were calculated for full sprays from figures which are considered representative of the practice using the range of coiling temperatures for various grades now specified by the Metallurgical Dept.

From Fig. 15, it can be seen that the present spraying system will not cool 0.105 gage below 1150°F; hence similar grades with heavier gages cannot be rolled even at the moderate speeds shown on the graph. At heavier gages, the coiling temperatures are still within the specified range but often the finishing temperatures rise to a point where the coiling temperatures are not obtainable.

In Fig. 16, a similar graph was drawn up with a reasonable increase in rolling speeds. The influence of 5, 10, and 15 additional top sprays are drawn below. The lengthening of the top spray area corresponding to 45 sprays appeared to be a reasonable extension to obtain the coiling temperature required. It was not considered necessary to add more bottom sprays, as the influence of this addition on the cooling will be quite small.

A greater drop in temperature on the runout table can be achieved not only by the lengthening of the sprayed area, as was considered previously, but also by increasing the pressure of the water. A comparison of the cooling by either an additional sprayed area or an increased pressure is indicated in Fig. 17. On this graph, a ΔT of 415° is obtained at a pressure of water 62.5 psig and full sprays (132.3-ft sprayed area). Below the line on the graph, changes of ΔT are obtained by variable pressures with constant length of sprayed area. Above the line, the

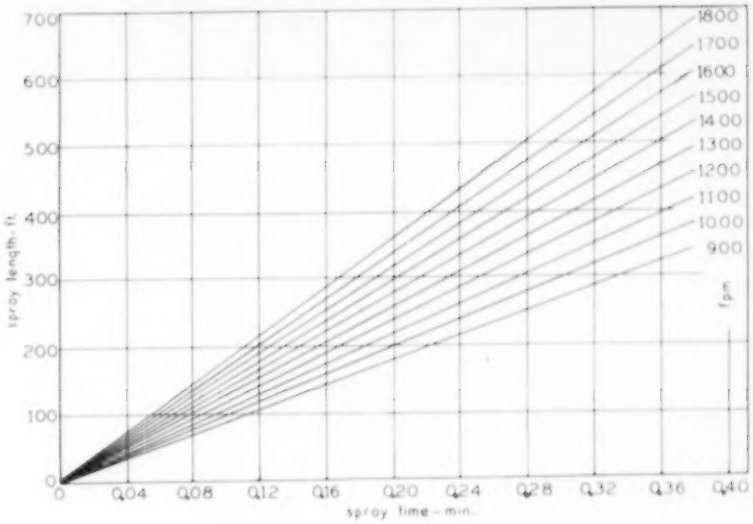

Fig. 20—Corrected spray times found in the preceding graph are translated into the actual distance covered by the top sprays, and therefore the number of top sprays required is determined. The runout table speed will affect this spray length, so a series of lines are presented for varying speeds in feet per minute.

same changes in ΔT are obtained by changing the sprayed area with constant pressure. As can be seen from the graph, for the range of a change in ΔT of approximately 50°, 1 lb of pressure is roughly equivalent to an increase of the sprayed length by 1 ft as far as the effect on cooling is concerned. Thus an addition of five sprays with the present spacing could have the same effect as an increase in pressure in the water main by 20 psig. It seems more economical to consider the addition of sprays, as this would not necessitate any change in equipment as had been done in Fig. 16.

In finalizing the possibilities of the full spray equation, a series of graphs, as shown in Figs. 18, 19, and 20, may be drawn up. In Fig. 18, gage and spray time are related to various finishing temperatures. Desirable ΔT values for finishing temperatures other than those specified in the graph may be corrected by means of the notation inserted at the side. In Fig. 19, the time under the sprays obtained from Fig. 18 is corrected for various flows. These corrected values of time can then be converted to length of the spray table required by means of the graph in Fig. 20. Thus, from these three graphs it is possible to calculate the length of the sprayed area, i.e., the number of sprays at a given spacing necessary to cool strips of known thickness, finishing temperature, and rolled at a known speed, down to the required coiling temperature. The figure for the flow, Fig. 19, will take into account either the pressure of the water for a given spacing of the sprays or determine the spacing of the sprays for a pressure available. Thus the graphs, Figs. 18 through 20, may be useful for the calculation of a runout table with any pressure and number or spacing of sprays.

Conclusions

1) The main factors affecting the drop in temperature between the continuous hot strip mill and the coiler are: a) the finishing temperature, b) the time of contact between water and strip, c) the pressure of the water, and d) the gage of the strip.

2) In cases where sufficient water is available, the width is of only minor influence on the cooling rate.

3) It seems that most of the cooling is achieved by the time of contact between water and strip. The pressure and, therefore, the flow per foot of the sprayed area, within the range of figures investigated, is of secondary importance. This indicates that the heat losses are mainly governed by a simple heat transfer from strip to water in direct contact and to only a much smaller degree by the impact of the stream.

4) The bulk of the cooling seems to be achieved by the top sprays.

5) The bottom sprays have a relatively smaller effect on the temperature drop. This also substantiates the suggestion that the time of contact between water and strip, and not the impact, is of primary importance.

6) An addition to the present runout table cooling system of 15 top sprays with the spacing now used should be sufficient in order to obtain desirable coiling temperatures, even at somewhat increased speeds and finishing temperatures.

7) A further increase of bottom sprays is not required.

8) An increase in top and/or bottom spray pressure is not necessary as compared with the present figures.

9) The nomogram provided in the report may be helpful for the calculation of all the elements of existing or new cooling systems.

10) The relationships obtained can be tied through the finishing speed and temperature of the hot strip mill to the roughing stand and furnaces, thus giving factors necessary to balance out and/or control the equipment from furnace to the coiler.

Acknowledgments

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Magnetite in the Hurley Copper Smelter

Three aspects of magnetite smelting are discussed. The first is the working out of equilibrium conditions for eliminating sulfur. The second is the influence of magnetite solubility on the difficulty of tapping the reverb matte. The third is an approximation of the equilibrium conditions in the reverb gases which govern whether magnetite is made or reduced in the reverb slag by these gases and by any iron sulfide in the slag.

by H. W. Mossman

MAGNETITE has had a varied history in the Hurley Smelter since its start in 1939. Magnetite determinations on the smelter products are made regularly only on the monthly composite samples. Variations on the monthly averages are shown in Table I. Magnetite which drops from the slag and matte in the reverb has some slight bottom buildup which comes and goes, but no substantial accumulation from this source has been found at the end of a normal nine months' furnace campaign. However, there has been some low grade magnetite bearing material mixed with considerable Al_2O_3 which has slid down from the bottom of the sloping flue between the reverberatory furnace and the waste heat boilers. This accretion has required drilling and blasting near the skimming end of the furnace. The magnetite has interfered with tapping at times. When the smelter was first started, tapping trouble from magnetite was extremely severe. Increasing the reverberatory furnace temperature by putting in an air preheater and a Dutch oven has helped greatly, although there still is occasional tapping trouble.

When the present series of physical chemistry articles on copper smelting started coming out in 1950, they were read with interest, but no immediate application was seen for them. Results of some laboratory work in 1952 aroused a much stronger interest in this physical chemistry. A series of melts was made on some converter slags, which had magnetite in very large grain sizes, with the object of reducing the grain sizes in the slag, as it was known that it was easier to handle in the reverb in that condition. Anything done in the tests greatly reduced the grain sizes—even in the controls, where nothing was done except melt the slag and cast it. There was more magnetite in the slags after the tests than before, and with wide variations. There were no obvious reasons for much of what happened in these tests. Much of the base material published in English in this field was made available for study. Recalculations were made on many of the type problems, and part of the data was reduced to local temperatures and compositions. Explanations were found for what happened in the 1952 series of tests on converter slags, and the same principles turned out to be a description of much of what magnetite does in the reverb. This article is to present the results of that study, from the viewpoint of applying the technical material in definite numerical form to the operating conditions in both the converters and the reverberatory furnaces at the Hurley smelter.

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TP 4300D. Manuscript, Oct. 19, 1955. New York Meeting, February 1956.

Table I. Magnetite Variations on Monthly Averages, 1939 to 1955

	Pct Magnetite		
	Lowest	Highest	Average
Converter slag	13.6	43.3	25.4
Reverb slag	2.7	20.9	8.7
Matte	2.8	15.9	9.6

In general it was found that magnetite is made or reduced in both the converters and the reverberatory furnace, depending on variations of temperature, matte composition, and reverb gas composition occurring in ordinary plant operation. Within reasonable limits, the field conditions for formation or reduction can be predicted, and probably can be set up and maintained. Converter conditions affecting magnetite formation can be put into numerical values better than for the reverb from purely technical calculations. The converter can be operated so as to keep the magnetite in the slag down to between 12 and 14 pct and still give satisfactory life for the converter brick. This depends upon having converter flux available which will make a slag with a good separation without raising the temperature too high. In the Hurley reverb and others with similar conditions, it is likely that a compromise of conditions will give a reasonably good control of combustion and still keep the magnetite from building up on the bottom.

This discussion consists of three main parts. The first is the working out of the equilibrium conditions in the converter for determining in which direction the reaction

$$3\ Fe_3O_4\ (s) + FeS\ (l) \rightleftarrows 10\ FeO\ (l) + SO_2$$

will go under actual converter operating conditions. The second deals with the influence of the solubility of magnetite in the slag and matte in the reverb on the difficulty of tapping matte. The third is an approximation of the equilibrium conditions in the

Table II. Tonnages and Assays, February 1952

Reverb	Tons per Day	Copper		SiO_2		Iron		CaO		Al_2O_3		Sulfur	
		Pct	Tons	Pct	Tons	Pct	Tons	Pct	Tons	Pct	Tons	Pct	Tons
Concentrates	813	21.9	178.0	6.3	51.2	29.5	239.8	0.10	0.8	1.9	15.4	39.4	320.3
Precipitates	78	84.0	65.5	1.5	1.2	3.0	2.3	0.12	0.1	2.9	2.3	0.8	0.7
Dusts	6	35.2	2.1	7.5	0.4	18.7	1.1	1.35	0.8	4.5	0.3	10.51	0.6
Limerock	63	0	0	4.96	3.1	0.45	0.3	49.9	31.4	1.84	1.2	—	—
Fettling	13	1.6	0.2	75.6	9.8	3.3	0.4	0.35	0	7.5	1.0	0.5	0
Total	973	25.3	245.8	6.8	65.7	25.1	243.9	3.4	33.1	3.3	20.2	33.1	321.6

Slags, Matte, Etc.	Tons	Pct Cu	SiO_2	Iron	CaO	Al_2O_3	Sulfur	Fe_3O_4
Converter slag	533	3.45	24.02	49.2	0.4	3.9	1.3	32.3
Reverb slag	629	0.44	33.4	39.2	4.0	6.4	1.7	11.6
Matte	720	33.57	0.6	35.6	0.1	0.9	24.4	10.0
Converter flux	180	same assay as reverb fettling.						

reverb gases which govern whether magnetite is made or reduced in the reverb slag by these gases and by any FeS in the slag.

To give a better understanding of conditions in the Hurley smelter, a brief history is given, with some illustrative data for assays, tonnages, and magnetite balances. The Hurley smelter handles wet charge. Natural gas is used for fuel, with 60 pct of the combustion air preheated to about 343°C (650°F). Combustion is started in a white walled chamber, or Dutch oven, 9 ft long, 42 in. high, and as wide as the furnace between two converter slag launders. The entering temperature is kept near 1539°C (2800°F). Two reverbs are used alternately, with furnace campaigns of about nine months, duration. There are three 13x30 ft horizontal converters. All the copper is fire refined. The composition of the charge varies greatly over the course of a year or two. The data given in Table II are for an average charge.

The idea formerly held about making magnetite in the converter was that when FeS was oxidized to FeO and SO_2 the FeO could not stay free for perhaps more than an instant, and that it then had to combine with SiO_2 to form slag, or with oxygen to make Fe_3O_4. That FeO had to go to magnetite if there was not enough SiO_2 present to slag it all was held to be axiomatic. Detailed field measurement seriously

Table III. Magnetite Balances in the Hurley Smelter

	Tons Per Day	Fe_3O_4, Pct	Tons Fe_3O_4 per Day	Copper, Pct
Average Month: January 1942				
Matte	604.0	13.1	79.1	39.47
Reverb slag	493.3	7.5	37.0	
Converter slag	417.0	28.0	116.8	
Reduced in reverb			0.8	
Newly made in converter			37.7	
Best Month: April 1954				
Matte	456	2.8	12.8	43.93
Reverb slag	426	6.6	28.1	
Converter slag	283	14.5	41.0	
Reduced in reverb			0.1	
Newly made in converter			28.2	
Worst Month: February 1947				
Matte	576	8.2	47.2	36.64
Reverb slag	463	10.3	47.7	
Converter slag	406	43.3	175.3	
Reduced in reverb			80.9	
Newly made in converter			128.6	
Converter No. 3, Charge No. 139, Jan. 5, 1953				
Individual Converter Charge, Input				
Matte put in, 195 tons at 8.6 pct Fe_3O_4			16.77	
Copper slag at start, 10 tons at 39.6 pct Fe_3O_4			4.00	
Cold stuff, 20 tons at 26.2 pct Fe_3O_4			5.34	
Flux, 51 tons at 0.8 pct Fe_3O_4			0.41	
Subtotal, not made in converter during charge			26.52	
Newly made in converter during charge			25.14	
Total input			55.66	
Output				
Slag, 180 tons at 28.7 pct Fe_3O_4			51.66	
Copper slag, 10 tons at 39.6 pct Fe_3O_4			4.00	
Total output			55.66	
Entire Smelter, Prorated to Tonnage of Converter Slag				
Matte	195	8.6	16.77	32.38
Reverb slag	205	7.3	15.00	
Converter slag	180	28.7	51.66	
Reduced in reverb			10.14	
Newly made in converter			25.14	

Table IV. Fe_3O_4 and FeO Distribution in Different Staggering Blows

Converter No. 3, Charge No. 139, Jan. 5, 1953

Blow No.	Fe, Tons per Min into Newly Made		Pct of Newly Oxidized Fe into Fe_3O_4	Ratio, Fe into Fe_3O_4 Compared to Fe into FeO	
	FeO	Fe_3O_4		Actual	Comparative
Fe_3O_4 and FeO Distribution in Four Blows					
1	0.0665	0.0113	14	0.1669	1.0000 (Base)
2	0.0749	0.0486	40	0.6489	3.8190
3	0.0518	0.0683	43	1.3185	7.7604
4	0.0242	0.0966	75	3.9917	23.4944

discredited this idea and pointed strongly toward some action in which the mass action of FeS is involved.

The study of making or reducing magnetite in the converter consisted in taking R. Schuhmann, Jr.'s comment[1] that the reaction $3Fe_2O_3$ (*s*) + FeS (*l*) $\rightleftarrows$ 10 FeO (*l*) + SO_2 might be reversible, and calculating the equilibrium conditions of the equation for ranges of temperature and material compositions which covered the variations in the actual converter operation at Hurley. It was found that this reaction did reverse under conditions commonly found in the converters, producing magnetite. The results could be plotted in terms of temperature and FeS concentration, see Fig. 1. Instead of using the concentration of FeS in Fig. 1, the percentage of copper in the bath or matte is used. This gives an index from the ordinary assays.

The calculations consisted of solving for a grid of equilibrium curves for different concentrations of FeS and getting this grid to intersect the a_{FeO} vs temperature curve for an iron silicate slag saturated with both SiO_2 and Fe_2O_3. This curve was plotted from Table II in Michal and Schuhmann's article in the July 1952 JOURNAL OF METALS.[2] An equilibrium curve was plotted for each of six different concentrations of FeS, varying from low grade matte to white metal. The equilibrium equation for this reaction is

$$k = \frac{a^{10}_{FeO} \, X \, p_{SO_2}}{a^{3}_{Fe_2O_3} \, X \, a_{FeS}} .$$

Calculations were made for a_{FeO}, and

$$a_{FeO} = \left[\frac{k \, x \, a^{3}_{Fe_2O_3} \, x \, a_{FeS}}{p_{SO_2}} \right]^{1/10} .$$

Partial pressure p_{SO_2} was taken as 0.12 atm, about its average value in the converters in the slagging period. The effect of raising p_{SO_2} to full converter blast pressure was studied, and it was found that this would raise the average temperature 4° (7°F), which is less than the probable error of field measurements.

The equilibrium constant was calculated for a series covering the range of possible field temperatures. In this case k goes up rapidly as the temperature rises. No values for activities of FeS were found in the references, and unmodified mol fractions were used. These mol fractions were calculated for the matte as Cu_2S + *X* FeS = 100 pct. The temperatures found for equilibrium for the copper percentage in the matte or sulfide bath in the converter are given in Table V. These figures are shown on Fig. 1.

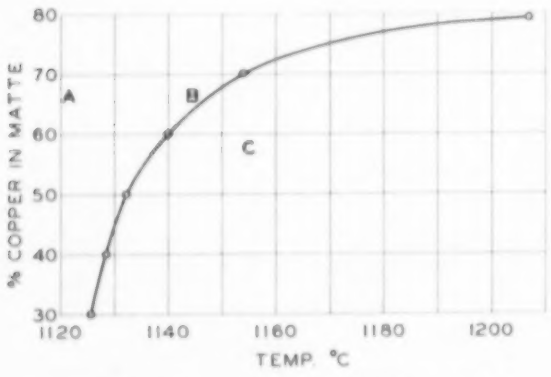

Fig. 1—Equilibrium curve for the reaction 3 Fe_2O_3 (*s*) + FeS (*l*) = 10 FeO (*l*) + SO_2 at contact of matte and an iron silicate slag saturated with SiO_2 in relation to temperature and percentage of copper in matte.

Table V. Equilibrium Temperature for Copper Percentage in Matte

Copper in Matte, Pct	Equilibrium Temperature °C	°F
30	1127	2060
40	1130	2066
50	1135	2075
60	1142	2087
70	1155	2110
79	1207	2205

To determine on which side of the curved line the reaction goes to the right and on which side it goes to the left, the activities equation for k is used. Holding everything constant except k and for the Fe_2O_3 to increase, k has to decrease or the temperature drop. Therefore, for any one point on the equilibrium line, magnetite is produced to the left and reduced to the right.

Discussion of Fig. 1

Line A—The slag is made at 1122° (2050°F), in the presence of about 10 to 12 pct Fe_2O_3 in the actual slag. From the operation of the last few years, with recording pyrometers on the converters, the slag as a general rule seems to be made at a temperature somewhat lower than this. At a temperature below the line *A*, this reaction does not apply, as it is conditioned on having a slag which is saturated with both silica and magnetite.

Line B—This is the equilibrium line for the slag saturated with both silica and magnetite. Between *A* and *B*, magnetite is made by the reversion of the reaction given. Saturation in an actual slag would be about 5 pct Fe_2O_3 at 1127° (2060°F), and about 15 pct Fe_2O_3 at 1210° (2225°F). Magnetite can be made to well above saturation in the zone *A* to *B*, with the extra amounts thrown out as solids. For one month, February 1947, the average magnetite in the converter slag was 43.3 pct.

Zone C—In zone *C*, the magnetite is reduced to just about the amount required for saturation at the temperature of the slag. With the air blast going into the converter bath, the conditions are so strongly oxidizing that the magnetite will never drop more than a nominal amount below saturation. In zone *C*, the magnetite can never get above saturation and force out any solid magnetite. For all practical purposes, to the right of line *B*, the percentage of magnetite closely approaches saturation—and stays there.

When crossing line *B*, from left to right, it is possible for a sharp and substantial reduction of magnetite to take place if the percentage present has built up above saturation.

It is obvious that the chart is not as accurate as if all the activities were known, or if it were worked out for a pure system of FeO-Fe_2O_3-SiO_2, but it is believed that it is close enough for reasonable guidance in the operation of a converter.

Two field checks on the validity of this chart are possible at Hurley from operating data. One is the rate of formation of magnetite in a converter charge which was sampled in detail. The other is the percentage of magnetite in the converter slag in relation to temperatures for a low month and a high

Table VI. Rate of Magnetite Formation in Different Blows in Converter No. 3, Charge No. 139, Jan. 5, 1953

No. of Blow	Relative Fe_3O_4 Formation Rate	Temperature				Copper in Matte, Pct	
		Start		Finish			
		°C	°F	°C	°F	Start	Finish
1	1.0000	1116	2040	1232	2250	32.7	75.1
2	3.8190	1121	2050	1216	2220	59.0	75.3
3	7.7604	1121	2050	1227	2240	68.5	76.7
4	23.4944	1121	2050	1232	2250	71.2	78.8

month. Table VI gives the rate of formation of magnetite in charge No. 139, converter No. 3 for January 5, 1953 in different blows in relation to temperature and matte composition. The bath temperature always started near the slag forming temperature and ended at approximately the reversion point. The temperatures and matte grades were almost a perfect fit for the zone between lines *A* and *B*. The longer the charge was blown on the high grade mattes (with little time in the temperature region which would have the magnetite down near saturation) the greater the production of magnetite. The slags varied from 19.7 pct Fe_3O_4 on the first skim to 32.3 pct at the end of the slagging period.

The recording pyrometers have furnished a confirmation of the accuracy of the reversion chart of Fig. 1. The lowest magnetite in the converter slag since the instruments were installed was 14.5 pct in April 1954. For that month the average of the peak converter slag temperatures was 1226° (2238°F), and the lowest peak for any one day was 1153° (2210°F). The peak temperature of every day of that month was above the 1207° (2205°F) shown by Fig. 1 as necessary for bringing the magnetite down to saturation at about 14 pct—and it was down to 14.5 pct. The highest magnetite since the recording pyrometers have been in use was 30.6 pct for May 1954. The average of the peak temperatures for that month was 1207° (2205°F). On 13 days the peak temperatures were below the point needed to make the magnetite go higher than saturation near the whitemetal stage, and on several days they were down to 1177° (2150°F). Where the temperatures indicated that the magnetite should have been above the saturation point, it was.

In applying these theories it was found that several operational factors would have to be considered. While the changes in the vapor pressure of SO_2 in the converter bath have only a negligible effect on the temperature of the reversion point, it cannot be overlooked that the vapor pressure of SO_2 affects the rate of the reaction when going from left to right on Fig. 1 and reduction of magnetite with the evolution of SO_2.[11] When SO_2 is being given off, the rate of the reaction deep in the bath is slow and reducing action of any consequence has to take place in the upper part of the bath or at the surface. When the reaction is going in the other direction and magnetite is produced, an increase in the vapor pressure does not cut down the rate, as both end products are solids.

The rate of formation of magnetite in the different blows might suggest the nonproduction of magnetite in a converter by a first blow plus transfer method. However, if the magnetite were ever below the saturation point in the converter slag in the first blow, it would certainly go at least to saturation at the end of the blow, no matter how carefully the temperature were regulated in reference to the matte grade according to Fig. 1. At skimming temperature this would be 10 to 14 pct Fe_3O_4. In the Hurley practice, the *copper slag*, as the granulated slag on the top of the copper at the finish of a converter charge is called locally, is left in the converter to be cleaned out by the next charge. This copper slag is high in magnetite, sometimes as high as 55 pct. There is enough free oxygen at the very end of a charge to change any iron present to Fe_3O_4 by direct oxidation. This copper slag keeps the converter slag for the first blow up to 25 to 30 pct Fe_3O_4, even with very little newly made magnetite in the converter during the first blow. With one such blow and three blows starting with a clean converter making magnetite only to the saturation point, the magnetite might be kept to an average of about 15 pct.

In a converter which is relatively clean of magnetite at the start, a low temperature could cause the formation of a large amount of magnetite. For a matte with 30 pct Cu, it would require about 1127° (2060°F) to keep the magnetite down to saturation. In Hurley practice, the slag is above saturation at the start, from the cleanup, and the temperature does not make much difference at that part of the charge. If the magnetite were lacking or low at the start and the converter were cold for awhile, a large amount of magnetite could be made.

Two changes were made in the Hurley converter department in 1952 which affect the making or reduction of magnetite. One was the installation of a conveyor system for fluxing the converters in the stack, instead of out of the stack. The other was the installation of recording radiation pyrometers on the converter, following the arrangements of L. L. Klein in Morenci, Arizona.

With the older arrangement of charging the flux into the converter while it was out of the stack, all the flux for an entire blow was put in at once, before any blowing could be started. That made the converter contents cold at the start of a blow. Optical pyrometer readings of as low as 1038° (1900°F) have been made at the start of a blow. These low temperatures caused the formation of much magnetite. One monthly average went as high as 43.3 pct Fe_3O_4. The use of conveyors all the way to the converters permits putting in any amount of flux at any time during a blow simply by pushing buttons. This makes it possible to keep almost all of the flux out of the converter at the start and permits letting the temperature come up to a considerably higher level, with flux available as needed. The average temperature at the start is now about 1121° (2050°F), which is slightly below the amount needed to keep the magnetite down to saturation for low grade mattes. The warmer starts have helped eliminate some of the worst magnetites. Having the flux available at any time makes it possible to approach almost any desired tempera-

ture without the danger of getting in the wrong amount of flux and having the converter either too hot or too cold.

The recording pyrometers were installed originally to help the operators avoid high temperatures and thus prevent deterioration of the furnace brick. The brick life was increased from about 20 million lb Cu repair to about 50 million lb. Operators were instructed to keep temperature below 1204° (2200°F) if a good separation could be attained at that temperature with the flux available.

The recorders, with a large index hand, are mounted where the converter skimmer can watch them from his regular working place. Signal lights are placed in the converter foreman's office. When the temperature is below 1232° (2250°F) a white light is on. Above that temperature a red light comes on. The indications are that, in the past, the converters were operated too cold most of the time, with occasional very high temperatures causing severe brick deterioration for a short time. The pyrometers last well, considering the severity of the service, and their use has definitely eliminated inadvertent very high temperatures.

It has been easy to keep temperatures down by adding plenty of flux. As can be seen from the illustrative assays, this flux is high in silica—75.6 pct —and is not diluted. References show that, while SiO_2 does unite with MgO, it requires a high temperature, 1500° (2822°F).[4] No converter temperatures are that high. It was found that the use of high silica material as *cold stuff* has produced excellent brick life.

The Santa Rita mine has a good supply of quartzite gangue material with copper in an oxidized form, which is not amenable to concentration by flotation, but which can have its metal extracted by use as a converter flux. The high silica slags work well in the converters. They make a creamy, fluid slag at as low as 1121° (2050°F), with a good separation. For a while after the pyrometers were first put on, some slags had to be raised to as high as 1277° (2330°F) to get a fluid slag. This was because some flux contained high alumina material from a dyke in the mine. This material has since been kept out of the flux. The present quartzite flux makes a very uniform low temperature converter condition possible.

The brick loss seems to be largely due to the solution of MgO by Fayalite, as shown in Fig. 6 of an article by Bowen and Schairer.[4] In this diagram, solution starts at 1177° (2050°F). Little solution takes place up to 1204° (2200°F). It then starts increasing, with the rate of brick loss increasing rapidly as the temperature rises. Some laboratory tests were made on the actual bricks and converter slags with the same results. From this material it is easy to see at approximately what temperature the converter should be run to keep brick wear down, and that a few occasional high temperatures could cause the loss of a large amount of converter brick in a short time.

When the converter slag temperature is kept low enough to save brick, there is a tendency to produce magnetite to above the saturation point. A fairly good compromise between saving brick and not making magnetite might be made by careful control of the temperatures. This would involve letting the temperature go up to 1127° (2060°F) as soon as possible at the start and then keeping the temperature on the right hand side of the line on Fig. 1. It would be best to make all skims except the last without trying to go all the way to whitemetal, as this would make it easier to keep the temperature down to the 1121° (2150°F) which is ideal for good brick wear. Of course, the last blow would have to be well cleaned up before going on finish, or the whitemetal blow. To keep this low temperature compromise under control would require flux and cold stuff which would make a slag that would be liquid at 1121° (2150°F).

At present, the Hurley converters are being operated too cold to keep the magnetite in the slag down to the saturation point. But they are not so cold as to make the high percentages of magnetite formerly made on isolated months, such as 35 pct or even up into the 40s. But, as will be discussed later, there are some conditions in a reverberatory furnace where more magnetite can be handled, without tapping or bottom trouble, than is represented by bare saturation of the converter slag with magnetite. One of these conditions exists now—a refractory charge which makes the furnace run very hot—and the reverb is doing very well with 22 pct Fe_3O_4 in the converter slag. Cutting the converter temperature down by charging a large amount of flux, without getting into trouble in the reverb, is profitable. When the shovels get into parts of the mine which produce a reverb charge that is less refractory, and the furnace does not run so hot, it may be necessary to run the converters hotter to reduce the magnetite. In April 1954, as mentioned before, the magnetite was 14.5 pct in the converter slag. This was secured without any experimenting, but by a reasonable increase in the converter temperatures. Therefore it is known that the converters can be operated to give a magnetite percentage in the slag low enough to give good tapping in a cool reverb without much sacrifice on either the converter brick life or the amount of converter flux.

Magnetite in the Reverberatory Furnace

The behavior of magnetite in the reverberatory furnace depends upon a balance of conditions, mainly the solubility of magnetite in the slag and matte and the formation or reduction of magnetite by the furnace gases. When the magnetite makes trouble in the reverb, it makes the matte hard to tap, or fills up the bottom. No effort is made to differentiate between the reduction of magnetite by cutting the oxygen down to a low enough percentage in reference to the temperature and any chemical reduction by FeS. The oxygen has to be brought down to a certain low level in reference to the temperature before the equilibrium conditions are right for the FeS to do any reducing. With the oxygen low enough to do the work alone, the FeS may lend a helping hand, but it is the amount of oxygen in reference to the temperature which determines what happens.

As the solubility of magnetite in the slag and matte is involved in much of the trouble in the reverb, the information which was available at Hurley on that solubility is covered in detail. Reference No. 5 gives the basis for a fairly good solubility curve for magnetite in converter and reverb slags, and that material is given in reference to temperature in Fig. 2. This chart is based on extending the $FeO + Fe_3O_4 + SiO_2$ to 100 pct for a large number of the slags at Hurley, and plotting these slags on the equilibrium diagrams given in reference 5 for a series of temperatures. The amount of Fe_3O_4 at

saturation can then be taken from the diagrams and calculated to Fe_3O_4. The chart is based on the assumption that the Al_2O_3, Cu, CaO, S, Na_2O, etc., do not take any Fe_3O_4 into solution.

The magnetite found in the slag is probably in solution in the Fayalite, or $2FeO\text{-}SiO_2$, contained there rather than in the slag as a whole; it is obviously not in solution in itself. When the magnetite goes high, there is not much iron left to go into Fayalite, and much of the SiO_2 remains free. It seems unlikely that the magnetite is in solution in free silica. With the 10.9 pct of residuals like Al_2O_3, CaO, etc. not considered as taking any magnetite into solution, there is nothing left to do the dissolving except the Fayalite and when the magnetite goes high, there is not much of that. Yet it is a matter of record that the magnetite does go into solution and, for some of the higher temperatures, in large amounts. Molten Fayalite is well known as very good solvent, especially at high temperatures. Table VII shows how the magnetite goes into solution in the Fayalite. The slag used for the calculations is an average of 28 months of converter slag, and averages are 25.9 pct SiO_2 and 89.1 pct $FeO + Fe_3O_4 + SiO_2$.

No references were found on the solubility of magnetite in matte. However, from one laboratory test and a detailed sampling of converter charge No. 139, converter No. 3 for January 5, 1953, data was obtained for the solubility of magnetite at 1177° (2150°F) in reference to the percentages of copper in the matte. From another laboratory test, the melting point of a 32 pct Cu matte was 971° (1780°F). Assuming that no solid solution is present, matte, when at the melting point, should hold no magnetite in solution. At low temperatures, the solubility of magnetite in slag was very nearly linear in relationship with the temperature. For a rough estimate of the solubility of magnetite in matte, a linear interpolation has been made in dotted lines, as shown in Fig. 3.

The Hurley reverbs are tapped near the burner end. There are two tap holes on each side, one 22 ft and the other 33 ft from the outside of the brick at the end of the furnace. While no exact percentage is available, by the time the first matte tap hole is reached, most of the reverb charge is smelted. Therefore, most of the matte originates between the burners and the matte tap holes. On a few months since the Hurley smelter was built, magnetite balances show that from 6 to 34 tons per day were made in the reverb. This would contribute some magnetite to the slag and matte. Usually, however, all the magnetite in the matte comes from the converter slag. A discussion of the action of solubilities in making good or bad tapping based on the converter slag alone is a reasonable analysis for the great majority of cases.

The distribution of the magnetite which is in solution, partly in the slag and partly in the matte, follows the general partition principle. If one is saturated with magnetite, the other is also. If solid magnetite precipitates out in one, it will also precipitate out in the other. Furthermore, if all the magnetite in one is in solution and at a certain percentage of saturation, all the magnetite in the other will also be in solution at the same percentage of saturation. These factors are based on a reasonable time for any required adjustment to take place.

In a direct charged reverb, there is no real control on the matte grade, and the matte temperature

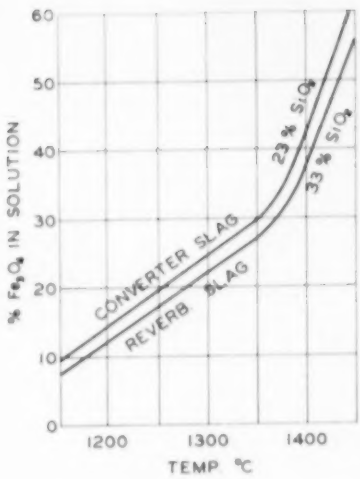

Fig. 2—Solubility curve for magnetite in iron silicate slags with $2\ FeO + SiO_2 + Fe_3O_4$—89 pct, in relation to temperature. Data after Schuhmann, Powell, and Michal.

does not change much. The amount of magnetite in the matte has to be controlled through the slag. From Fig. 2 the temperature to be maintained in a slag to keep a certain percentage of magnetite in solution is obvious. With that taken care of, the matte will have a magnetite content at the same fraction of the saturation amount.

The charge only contributes a nominal amount of magnetite in a direct charged reverberatory furnace. Sometimes samples are secured of matte which was made on top of the charge piles and ran out of a hole in the sidewall of the furnace. This *original* or sidewall matte has from 0.0 to 0.4 pct Fe_3O_4 in it. As a consequence, when this original matte gets down in the reverb bath it is a good solvent for magnetite.

As long as all the magnetite in the matte is in solution, the matte taps well regardless of the magnetite content. If solid magnetite occurs in the matte, hard tapping results. The more solid magnetite there is, the harder the tapping becomes. It is fairly easy to tell when solid, or crystalline, magnetite occurs in the matte. Even a rough polishing on a fine grained grinding wheel will reveal primary magnetite in a test specimen. If all the magnetite were in solution, and the matte were quenched quickly enough, no magnetite crystals would appear. Because of its explosive nature, matte has to be cooled slowly and secondary crystals are formed. The secondary crystals come from the magnetite which was in solution. While secondary crystals are small, primary crystals are larger. The more solid magnetite, the larger the primary crystals become, with no change in size in the secondary ones. The two sizes are easy to distinguish. When freshly ground, magnetite has a steel gray color with a high luster, while matte is black and has almost no luster. The magnetite is hard; the matte is soft. With a rough polishing, the matte will tear away, leaving pits, while the magnetite will stand out as humps or ridges. If there is enough solid magnetite to cause the matte to be hard to tap, a X5 pocket magnifying glass will resolve the primary magnetite crystals. When the tapping gets very bad, these primary magnetite crystals can be seen on the matte launder skulls like large, steel-gray grains of sand.

The dilution of the magnetite in the converter slag when it enters the reverb varies widely. On the illustrative month it was 18 pct. After the correction for this dilution, the magnetite in the reverb

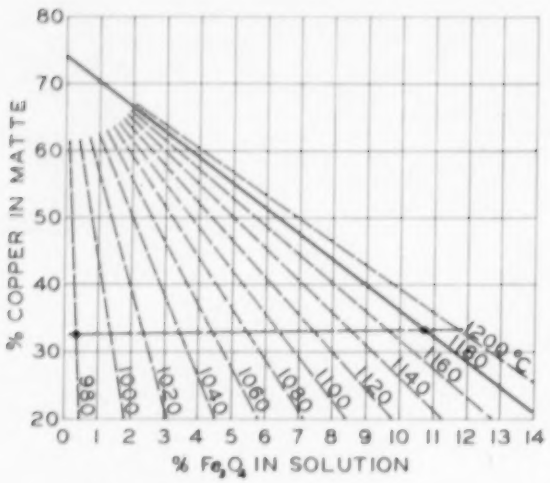

Fig. 3—Solubility of magnetite in matte in reference to temperature and percentage of copper in matte.

slag near the tap holes has to be under saturation to keep all the magnetite in the matte in solution. Before the preheater was installed the reverb slag at the tap holes was often above saturation, and solid magnetite in the matte was almost a daily occurrence. Since the preheater was installed the magnetite in the matte has usually been in solution. However, there have been periods when solid magnetite has appeared in the matte. The ore in the mine varies considerably, and in 1948 the concentrates were very soft and easily smelted. Insol assays went down to as low as 4 pct. This soft charge did not make a white scum over the bath, but left a black surface on the slag. The black surface absorbed heat so fast that a high temperature could not be maintained, even with preheated air. A temperature of 1310° (2410°F) was common on the top of the bath at the matte tap holes. This would take only about 24 pct magnetite into solution, and some of the monthly averages were higher than that. Bad tapping was frequent when Fe_3O_4 was above the average. That lasted a year or so, and then the insol went up enough to make a white topped bath, temperatures went up, and tapping trouble stopped. The combination of black topped bath and high magnetite still occurs occasionally and makes trouble. What is presumed are rather rare occasions of extremely high magnetite still make hard tapping.

The amount of magnetite held in solution in different parts of the slag in the reverb varies at Hurley. The situation at the matte tap holes is a local condition, with the matte removing a large amount of the magnetite from the reverb in a short time. Toward the skimming end, where the temperature gets down to about 1232° (2250°F) as a low and 1349° (2460°F) for charges with high insols, the Fe_3O_4 gets as low as 2.7 pct and as high as 20 pct in the slag for a monthly average. At 1232° (2250°F) the slag would hold 15 pct Fe_3O_4 in solution.

So far, magnetite determinations have been made regularly only on the monthly composite samples. No doubt there have been instances not covered by assays where the magnetite has gone past the above limits. Some bottom buildup occurs, mostly on high grade mattes, which does not seem to be associated with the sliding in of low grade magnetite material from the bottom of the sloping flue between the reverb and the waste heat boilers. When the matte grade goes down, the *solids* on the bottom gradually disappear. The almost magnetite-free original matte, as it flows back from the 50 or 60 ft of charge pile which extends past the tap holes, does a good job of cleaning the bottom of any occasional magnetite which falls there and also takes much magnetite out of the reverb slag.

The washing action of this matte, past the end of the charge pile, has its limitations. For the first 19 campaigns of the smelter, it kept the skimming end of the furnace clean. During that time the fettling for the bottom of the flue was put in with wheelbarrows from runways near the top of the flue roof. The material was dropped into very low hoppers only a foot above the roof and it did not fall very far. The small impact from this short fall kept the fettling from sliding into the reverb and making an accretion on the bottom. In 1950 an overhead crane was installed to charge this material, and the fettling is now dropped quite a distance from an upper floor. The much greater impact makes the fettling slide along the bottom of the flue and down into the reverb, carrying the dust from the bottom of the flue with it. This dust has as much iron in it as the original charge, and much of this iron goes to magnetite while in the flue by direct oxidation. The result is a low magnetite, high alumina accretion on the bottom of the reverb at the skimming end. Assays are given for the two accretions which have been dug out in Table VIII.

Table VII. Magnetite in Solution in Fayalite in Converter Slags, with 23 Pct SiO_2

Temperature		Pct Fe_3O_4 in Solution in Slag	Pct Fe_3O_4 in Solution in Fayalite	Free SiO_2
°C	°F			
1149	2100	5.2	6.3	2.0
1204	2200	12.1	17.2	5.2
1260	2300	19.6	33.5	8.7
1316	2400	27.4	59.8	12.4
1371	2500	36.4	115.3	16.5
1426	2600	49.8	483.5	22.9

Table VIII. Composition of Accretions on Reverb Bottom

Reverb	Year	Pct						
		Cu	SiO_2	Fe	CaO	Al_2O_3	S	Fe_3O_4
No. 2	1950 to 51	7.0	12.9	43.4	3.0	12.7	—	36.5
No. 1	1953 to 54	11.8	9.5	41.0	1.4	17.3	3.7	40.4

Chemical Reduction and Formation of Fe_3O_4 in the Slag in the Reverb

Magnetite is sometimes reduced in the reverb, while at other times it is produced. The oxygen content in the combustion gases determines which occurs. When magnetite is made in the reverb slag, the combustion gases supply the needed oxygen. However, when magnetite is reduced in the reverb by chemical action, it is impossible to separate the reduction by the withdrawal of oxygen from the magnetite by the combustion gases, from the reduction by FeS. However, even if and when such reduction is made by FeS, the amount of oxygen in the combustion gases, along with temperature, determines whether magnetite is made or reduced in the reverb slag.

Chipman and Marshall[9] mentioned this chemical reduction or formation of magnetite in 1940. Though that was of considerable scientific interest, the first references which could be readily applied to conditions in a copper smelter were those written for iron and steel plant conditions by Darken and Gurry,[7,8] and published between 1945 and 1948. The general subject has been directly applied to copper smelter conditions by Schuhmann, Ensio, Powell, and Michal[1,2,10,11] from 1950 to 1953.

In general, these articles cover the measurement of the oxygen which passes from ferric to ferrous oxide as one changes to the other in molten mixtures of various sorts, in reference to the temperature. The investigators made melts containing both of those oxides, and at times other substances, inside a gas-tight inclosure. Then they passed a mixture of CO_2 and CO over the surface of the melt, and at a constant temperature varied the ratio of CO_2 and CO until equilibrium was reached, as shown by the same percentages of CO_2 and CO coming out as went in. The amount of oxygen involved could then be calculated from the reaction $2CO_2 = 2CO + O_2$. In a few cases other gases were used, but the other determinations do not apply so well to reverberatory furnace gases. By now this amount of oxygen over a range of compositions and temperatures is well established for copper smelter slags. The amount of oxygen is very small, only about 0.000,000,03 pct by volume for a slag saturated with both SiO_2 and Fe_3O_4. That is the most oxidizing condition taking any part in determining in which direction the reaction goes.

Of course, it is very easy for the oxygen to go higher, but that would only force more solid magnetite to be precipitated out. At the most reduced condition, when the slag is in equilibrium with metallic iron, the oxygen is just about one millionth of this amount. Small as this amount is, the conditions determined by it have much to do with what happens with magnetite in copper smelters. As applied to actual operating conditions, it is impossible to reach the most reducing condition in a furnace which is heated by burning fuel in it, though the gas conditions at the point where the slag becomes saturated with magnetite can be secured in the Hurley reverb. Whether the oxygen composition is under, at, or above the amount necessary to bring about saturation is of commercial importance.

Equilibrium with both oxides present in a molten slag in a furnace is not a static or dead condition. It does not mean that there is a certain amount of FeO and Fe_3O_4 and no oxygen going back and forth. There is as much of one going in one direction as there is of the other going in the opposite direction. For any equilibrium condition, there is a certain volume of free oxygen inside the molten slag. This free oxygen in the slag has unobstructed communication with the oxygen in the gases above the slag. When equilibrium is reached there is the same amount of oxygen going in both directions across the phase boundary. As far as this small amount of oxygen is concerned, the gases in the furnace influence all the action to the bottom of the slag layer. Therefore, the gases in the reverb do more than supply heat; they are an integral part of the chemical system which determines how much magnetite is in the reverb slag. If possible, the combustion should be regulated to control magnetite as well as the burning of the fuel. Fortunately, a reasonable compromise between the two is likely with combustion gases like those found in the natural gas fired reverb at Hurley.

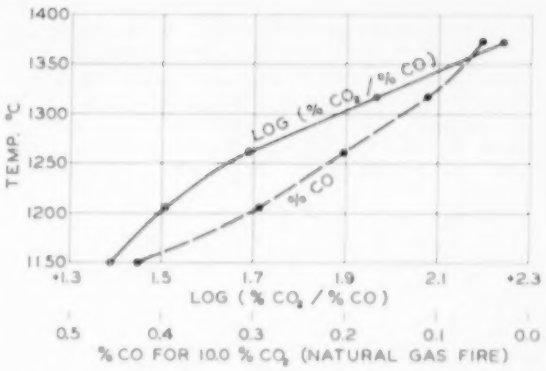

Fig. 4—Magnetite saturation curve for iron silicate slag saturated with SiO_2 in relation to temperature and log (p_{CO_2}/p_{CO}) and in relation to temperature and percentage of copper for 10 pct CO_2 (for natural gas fire). Data after Darken and Michal.

In the references the results are expressed in terms of log (p_{CO_2}/p_{CO}). The log is to the base 10 and p is the partial pressure in atmospheres. For all practical purposes, the percentages by volume can be used just as well as the partial pressures in operation, as only the ratio is required. Charts are published which show this log in reference to pure gases and a pure system of $FeO + Fe_3O_4 + SiO_2$. Fig. 3 of reference 11 shows the chart which applies best to the copper smelter slags. In Fig. 4 this chart has been changed and applied only to magnetite at its saturation point. The saturation is based here on a total of 89.1 pct $FeO + Fe_3O_4 + SiO_2$, which was found to be the average of a large number of Hurley slags. A line shows the CO required for magnetite saturation with the ordinary 10 pct CO_2 found in the combustion of natural gas. The log varied from +1.39 at 1149° (2100°F) to +2.24 at 1371° (2500°F). This means that for pure gases the slag would be saturated with magnetite at 1149° (2100°F) with 25 times as much CO_2 as CO, and at 1371° (2500°F) with 191 times as much CO_2 as CO. At 1149° (2100°F), with 10 pct CO_2, it would take 0.44 pct CO to bring the Fe_3O_4 to saturation, and at 1371° (2500°F), it would take only 0.02 pct CO.

In trying to apply this principle to an operating furnace, it was uncertain whether the curve for a pure slag and pure CO_2 and CO would fit, as the slag is not pure $FeO + Fe_3O_4 + SiO_2$ and the gases are far from pure CO_2 and CO. After studying the application of this theoretical material to the field measurements in the Hurley furnaces, the conclusion was that the general shape of the curve was a fairly good explanation of what took place in the reverb.

In attempting to find out the percentage of CO in the reverb which would show conditions needed for saturation of the slag by magnetite, it was plain that: 1) if there was any free oxygen at all in the gases next to the slag, as shown by an ordinary commercial gas analyzing machine which reads to 0.1 pct, the slag would go to saturation if given enough time to reach equilibrium, and 2) as the amount of oxygen needed was so small, the amount of CO probably would be small.

The chief check on the amount of CO at which the slag went below saturation, or approached saturation, was from the study of the general combustion and magnetite history of the reverberatory

furnace. Usually there was a fairly large amount of CO in the gases next to the bath in the center of the furnace—up to 0.6 or 0.8 pct. Sometimes free oxygen was present. There were times when the bottom would build up at the skimming end, and then become clean. However, from 1939 to 1950 the bottom was always clean of purely magnetite deposits at the end of the campaigns. Therefore, it generally seemed as if about 0.6 pct CO would keep the bottom clear of magnetite.

For another check on the amount of CO needed to keep the magnetite down to the saturation point, the combustion records were searched, and a number of typical months were selected. The furnace gases for those months had a range of CO from high to zero, but no free oxygen. The composition of the gases in contact with the surface of the slag at the center of the furnace was considered to be the key figure. Then magnetite balances were worked out for those months. The tons of magnetite made or reduced per day in the reverb were plotted against the percentage of CO. The values were close to a straight line. The point at which no magnetite was made or reduced was 0.43 pct CO, taking things as they came in the furnace, with some free oxygen on the sides. If the free oxygen were eliminated from the sides of the furnace, this CO percentage would probably be reduced.

These two checks on the amount of CO are not so much for an exact point at which saturation is reached as for the amount of CO above which the magnetite is definitely reduced and below which magnetite is made.

So many months had CO percentages up around 0.6 or 0.8 pct because from 1939 to 1950, due to the high draft losses in the waste heat boilers as originally installed in 1939, there often was not enough draft at the reverb offtake to burn the desired amount of fuel and still keep the suspended roof of the flue between the reverb and the waste heat boilers under suction with no CO in the exit gases. That particular roof had to be kept under suction or the metal suspension devices would burn out. Making a fairly high CO by forcing the fuel without supplying enough air for perfect combustion was tried and worked fairly well. The thermal efficiency of the furnace was good with the high CO, and the roof was kept under suction and *lived*. The CO burned by the time it reached the boilers, and did increase slagging on the front row of boiler tubes, but the boiler tubes required very few replacements.

Since September 1950, revisions in the boiler installation have produced enough draft to put all the desired forced draft air into the reverb without putting a pressure on the suspended roof of the flue. Most of the time there is a large excess of draft. Eliminating all the CO next to the bath is avoided. The combustion is adjusted to get at least 0.4 pct CO next to the bath in the center of the furnace. If the CO runs a little higher than that at times there is no concern about it, but very high CO percentages are now avoided.

The chemical reduction of magnetite by lowering the oxygen concentration in the gases above the slag by the direct withdrawal of an oxygen atom from the Fe_3O_4 in the magnetite, would work very well on large percentages of magnetite, such as 30 pct Fe_3O_4. It could continue working down to the saturation point of magnetite in the slag, and to about 12 or 13 pct Fe_3O_4. However, to force the magnetite to a lower percentage by this reaction would require too much CO to be practical, as it would require 1.0 pct CO or more to set up the needed equilibrium conditions.

The chemical reduction of magnetite by FeS in the reverb slag fits about the same limits. For this type of reduction at the matte-slag interface in the reverb, the static pressure of the slag layer would cut the rate of the reaction at that point so low that its effect would be negligible. For the particles of FeS at or near the surface of the slag this reduction might be important. The same reaction would be involved here as in the converter $3Fe_3O_4 + FeS = 10FeO + SO_2$. There would be little FeS, and the equilibrium would be the same as for matte just before whitemetal is reached. Starting with magnetite above saturation, it could go down to saturation at 1213° (2215°F). To go to less than saturation would require a temperature of 1349° (2460°F) or higher, because as the magnetite goes from saturation to less than saturation, by even a small amount, there is a large change in the value of the activity of the magnetite, which is reflected in the results of the equilibrium calculation. The highest reverb slag temperature measured at Hurley was 1288° (2350°F), and that only when an unusually refractory charge was being smelted. A temperature of 1232° (2250°F) is common. Therefore, the FeS reduction of magnetite in the Hurley reverb slag cannot take place below the saturation percentage of magnetite in the slag. However, with the oxygen concentration as low as it often has been, the FeS in the slag could do a large amount of chemical reduction of the Fe_3O_4 from large percentages to the saturation point. Both the chemical reduction by lowering the oxygen concentration and the reduction by FeS can cut high Fe_3O_4 percentages down to the saturation point. Oxygen concentration and temperature determine the conditions under which this is possible.

In either case, it would be desirable to have enough free SiO_2 present to take up any FeO released by the reduction of Fe_3O_4, as that would create a slag which would be fluid at a low temperature and a good solvent for the magnetite which would remain.

Magnetite percentages well below the saturation amount are usual in the Hurley reverb slags. Reducing the magnetite from the saturation point of approximately 13 pct at the common temperatures to about 2.7 pct minimum for a monthly average comes from the matte dissolving the magnetite out of the slag. In the Hurley reverb, with tapping near the burners, the matte acts as a counter-current washer. At the skimming bay end of the charge zone the newly made matte can have practically zero magnetite. In the Hurley furnace the matte-fall is very large. The almost magnetite-free matte, flowing back under the slag, can dissolve a large amount of magnetite which goes to the converters in closed circuit. When the slag does not have too much magnetite in it at the start, final low percentages of magnetite are understandable.

To keep the magnetite off the bottom in the area 40 ft past the end of the charge zone, in the skimming bay end, a substantial ebb and flow of new matte into this dead end might be required. However, at Hurley, where a large tonnage is smelted in one reverb and tapping is very irregular, a large amount of matte washes into and out of that area.

In some calcine charged reverbs, the slag *mushes up* and magnetite drops on the bottom of the furnace unless the SiO_2 is kept up to about 32 pct. If the

SiO_2 is raised, the slag thins and the magnetite no longer drops out. At the Hurley smelter, the SiO_2 in the slag has gone down to as low as 30 pct with none of this trouble. The difference in behavior probably comes from an inherent difference in the makeup of the two kinds of charges.

The slag forming material in a calcine includes some iron oxide. In the Hurley charge of concentrates and copper precipitates there is no iron in the slag forming material. There are several methods of calculating reverb charges from the percentages of SiO_2, Al_2O_3, FeO, etc., but none of them is very good. In some recent test melts at Hurley, it was obvious that some of the minerals did not break down into free SiO_2 and Al_2O_3 under reverb furnace conditions. Extending this finding to the calculation of a calcine charge by any of the methods, there would be more free FeO and less free SiO_2 than anticipated. This is thought to be what happens in a calcine slag that *mushes up* with a low SiO_2 which, from a magnetite making standpoint, is bad. The excess free FeO could go to magnetite by direct oxidation if there were even a trace of free oxygen in the furnace gases. To help by raising the SiO_2 to some arbitrary percentage, a flux with a large amount of free SiO_2 would have to be used. This arbitrary percentage of SiO_2 looks like an indirect index of how much free SiO_2 is required.

In the Hurley reverbs there is a zone of gases on the sides which have free oxygen even when the gases in the center have no oxygen, but some CO. Some magnetite is made in this zone, as shown by samples of the slag taken across the reverb, making a localized zone on the sides where the slag is higher in magnetite. What happens then depends upon a balance of factors. There is some diffusion of magnetite through both the slag and the matte, and some solution. If the matte washout is good enough, the bottom is kept clean. So far in the local reverb history, there have been no side buildups of magnetite.

Some magnetite is formed on the fettling piles on the sides of the reverbs from the normal dusting of the reverb. There is some dusting, even with wet charge. The dust sticks there and, with some free oxygen always in the gases on the sides, magnetite would be made on the face of the fettling piles. So far, that has given no trouble.

Magnetite could be made very readily by the exposure of molten Fayalite on the face of the reverb charge piles to even a small amount of free oxygen in the furnace gases. The time of exposure while the molten material is running off should be enough to make an appreciable amount of magnetite. In the 1952 converter slag tests analyses of the furnace gases were not made. However, on one test where the atmosphere is believed to have been oxidizing, the magnetite increased at the rate of 2.5 pct per min. In only one of the tests thought to have been in an oxidizing atmosphere the magnetite increase dropped below 1.0 pct per min. In any reverb charge which leaves some molten Fayalite on the face of the charge pile for even a short time, as a calcine charge or a direct charge with some converter slag in it, some of the iron silicate could be broken down by free oxygen and the FeO made into magnetite. From the Hurley experience in taking cross sectional gas samples in the reverb, the presence of some free oxygen on the sides of the furnace is regarded as probable when the gas composition in the center of the furnace represents nearly perfect combustion. One of the findings of the recent investigations into the physical chemistry of the silicates is that while $2FeOSiO_2$ is formed in copper smelter slags, and is found as the mineral Fayalite when the slags are quenched, the FeO and the SiO_2 are not tied together very strongly in a molten slag and are easily broken apart in an atmosphere with only a minute quantity of free oxygen.

Summary

Background material has been given for the smelter. Then a *reversion chart* for the reversion of the reaction $3Fe_2O_3 + FeS \rightarrow 10FeO + SO_2$ is given in relation to temperature and matte composition. It is shown that the temperatures and matte compositions required for reversion occur in the ordinary converter practice at Hurley, and that magnetite formation follows the implications of the chart. In the reverb, solubility charts are given for magnetite in matte and reverb slag, and the distribution of magnetite between the two according to the partition principle is discussed. The *washing* of magnetite off the reverb bottom toward the skimming end by matte which was originally magnetite-free in a direct charged reverb is pointed out. The formation and reduction of magnetite in the reverb slag according to the value of the log (p_{CO_2}/p_{CO}) in the combustion gases is developed. From the history of the reverbs at Hurley it is shown that the magnetite in the reverb slag can be kept near the saturation point by amounts of CO which have worked well in a natural gas fired furnace, and then brought down to even lower percentages by the counter-current washing of the low magnetite matte.

Acknowledgments

Thanks are due the Kennecott Copper Corp. for permission to publish the data in this paper. Thanks are also due Charles L. Lockart for his assistance in discussing the basic physical chemistry involved, and for checking some of the earlier calculations; to James H. Halley for checking the actual calculations used, and to H. G. Bower, Editor of the *Chinorama*, for correcting and rewriting the manuscript.

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Discussion of this paper sent (2 copies) to AIME by Nov. 1, 1956 will appear in AIME *Transactions* Vol. 209, 1957, and in JOURNAL OF METALS, October 1957.

Basal Plane Development in Electrodeposited Hexagonal-Close-Packed Metals: Zinc, Titanium, and Zirconium

The object of this paper is to show the manner in which typical electrodeposits of hexagonal-close-packed metals—zinc, titanium, and zirconium—tend to form. The conditions of electrodeposition markedly affect the nature of the deposits in regard to crystal size, orientation, and appearance. No attempt is made to correlate cell conditions with deposit quality but, rather, characteristic types of deposits were selected and a study made to show the relationship between outward appearance and crystal structure. The zinc deposits were made in aqueous electrolytes and the titanium and zirconium deposited from fused salt baths.

by W. R. Opie

ZINC deposits studied were made in the American Smelting and Refining Co.'s Central Research Laboratories. They were representative of bright, shiny, smooth deposits, which can be obtained by using proper current densities and electrolyte addition agents, and of deposits which are dull and tend to be nodular. Four deposits were selected which ranged from smooth and bright to very nodular and dull, and their classifications are shown in Table I. These were examined microscopically and were found to differ quite markedly in outward surface structure and in microstructure. The outward surface variation is illustrated in Figs. 1 to 4. At X50 the texture could be seen to progress from granular to platelike to smooth. The microstructures of the deposits' cross sections from starting sheet to outer surface are shown in Figs. 5 through 8. The dull nodular deposits (sample Nos. 1 and 2) show some tendency for acicular growth. The platelike sample (No. 3) shows tendency for columnar growth development and the smooth deposit (No. 4) has a well developed columnar structure.

Back-reflection X-ray patterns of the deposit surfaces revealed some interesting information. The crystal orientation was random in all deposits except the smooth deposit. Fig. 9 is a typical back-reflection pattern of a nodular deposit. All planes are reflecting in the proper relative intensity. The pattern of the smooth deposit is shown in Fig. 10. The only planes which reflect are the {10.4}. This series of planes is in the right position, with the $FeK\alpha$ radiation used, to satisfy the Bragg equation if the basal planes {00.1} are at right angles to the X-ray beam. The results were interpreted to mean that in the bright smooth deposit preferred orientation has occurred with the basal hexagonal planes oriented in the plane of the sheet but with all other planes randomly oriented around the c-axis.

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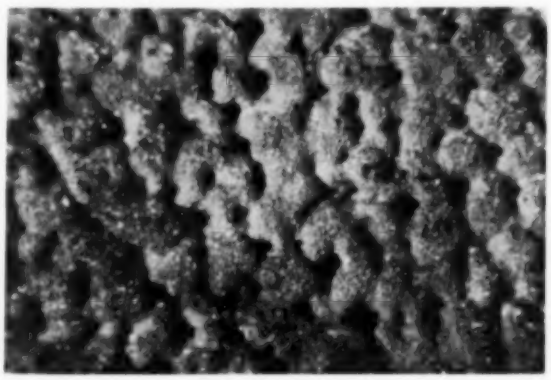
TP 4295DE. Manuscript, Nov. 15, 1955. Cleveland Meeting, October 1956.

Fig. 1—Very nodular electrolytic zinc deposit. Appears dull to the naked eye. X50. Reduced approximately 35 pct for reproduction.

Fig. 2—Nodular electrolytic zinc deposit. Appears dull but smooth compared to the deposit shown in Fig. 1. X50. Reduced approximately 35 pct for reproduction.



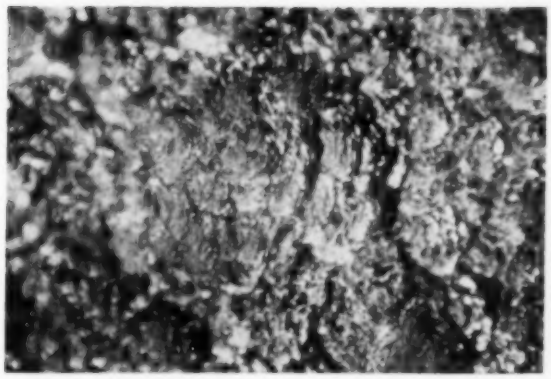


Fig. 3—Platelike electrolytic zinc deposits. Appear shiny to the naked eye. X50. Reduced approximately 35 pct for reproduction.

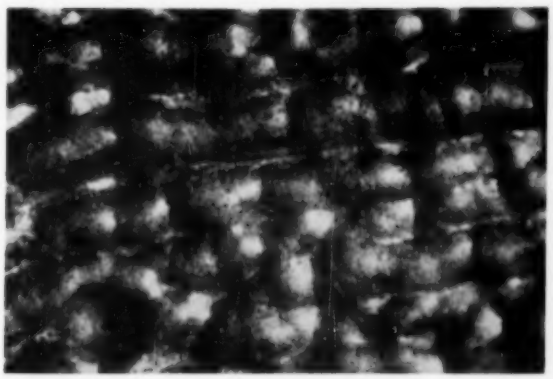


Fig. 4—Smooth electrolytic zinc deposit, slightly mammillar. Appeared very bright and smooth to the naked eye. X50. Reduced approximately 35 pct for reproduction.

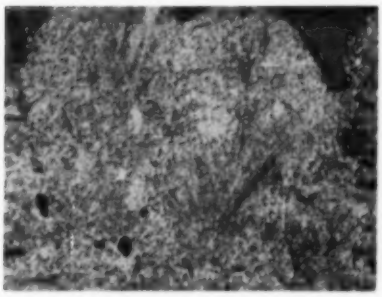


Fig. 5—Microstructure of very nodular deposit. Sample No. 1. The starting sheet surface is toward the bottom of the figure. X50. Reduced approximately 30 pct for reproduction.

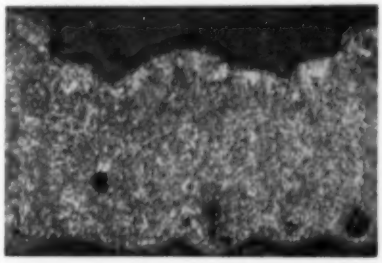


Fig. 6—Microstructure of nodular deposit. Sample No. 2. The starting sheet surface is toward the bottom of the figure. X50. Reduced approximately 30 pct for reproduction.

Fig. 7—Microstructure of shiny platelike deposit showing tendency for columnar growth development. Sample No. 3. The starting sheet surface is toward the bottom of the figure. Reduced approximately 30 pct for reproduction.

Fig. 8—Microstructure of bright smooth deposit. Sample No. 4. Columnar growth is well developed. The starting sheet surface is toward the bottom of the figure. X50. Reduced approximately 30 pct for reproduction.

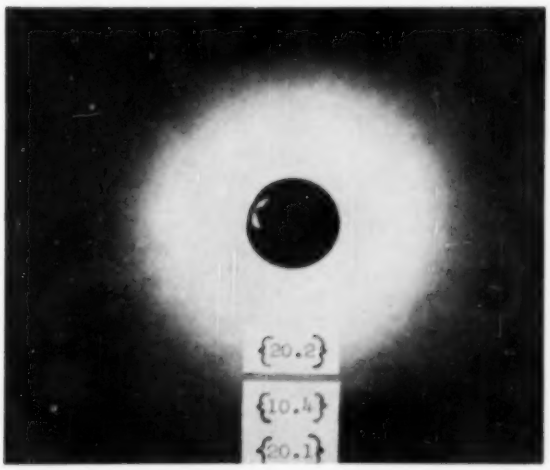


Fig. 9—X-ray back-reflection pattern of nodular deposit. Sample No. 2.

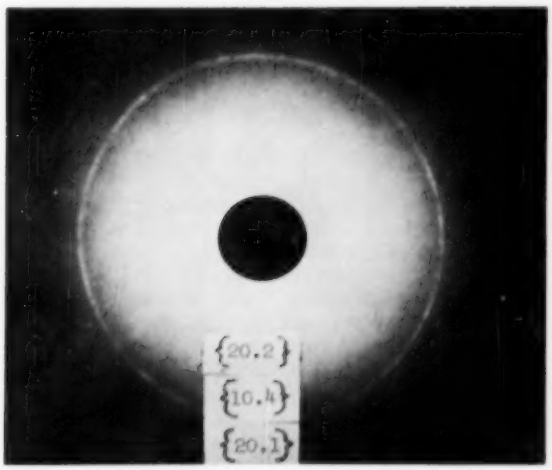


Fig. 10—X-ray back-reflection pattern of smooth bright deposit. Sample No. 4.

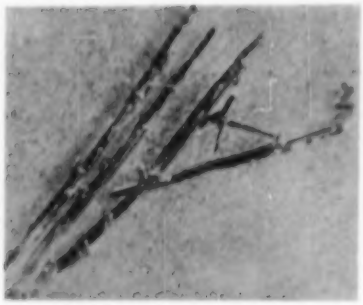


Fig. 11—Acicular and stepwise growth in electrodeposited titanium metal. The growth usually is in a direction displaced 30° to 35° from the c-axis. It is possible that growth is proceeding along a unit cell face diagonal, such as the (110) direction, which is 32.2° from the c-axis. X15. Reduced approximately 35 pct for reproduction.

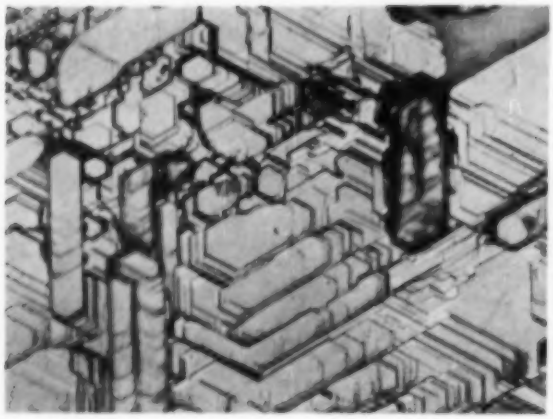


Fig. 12—Basal plane development in platelets of titanium deposited below the α-β transformation. X50. Reduced approximately 30 pct for reproduction.

This tendency for basal plane development was noted also in titanium and zirconium deposited electrolytically from fused salt baths at temperatures below the critical temperature at which, upon cooling, the body-centered-cubic structure transforms to hexagonal-close-packed.

Table I. Classification of Zinc Electrodeposits

Sample No.	Description
1	Very nodular
2	Nodular
3	Platelike
4	Smooth

Titanium Deposits

The specimens selected for study were made in the Research Laboratory, Titanium Div., National Lead Co., by electrolysis from fused salt baths at temperatures below 880°C (critical temperature for titanium). Conditions are such in fused salt electrodeposition that development of individual crystallites are favored in contrast to massive plates which are easily deposited from aqueous electrolytes, e.g., the zinc deposits discussed previously.

Three types of crystal development were noted in titanium:

1) An acicular growth in a direction displaced 30° to 35° from the c-axis. The needlelike crystals formed at times are hexagonal prisms. Fig. 11 illustrates this type of growth.

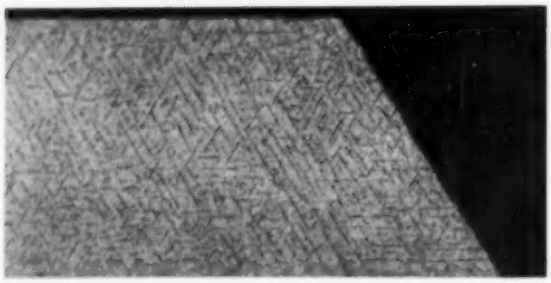


Fig. 13—Microstructure of the basal plane of a well developed hexagonal crystal of electrodeposited titanium metal. Etchant used was glycerine with HF and HNO_3. X250. Reduced approximately 30 pct for reproduction.

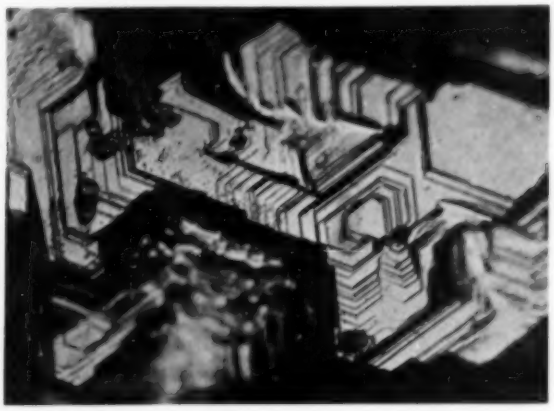


Fig. 14—Platelike growth in zirconium metal deposited below the α-β transformation temperature. X75. Reduced approximately 30 pct for reproduction.

2) Block-type growth. Growth tends toward the development of hexagonal prisms but is periodically interrupted by growth along the basal plane. The net result is a series of hexagonal blocks displaced from each other a small distance on the basal plane. Typical crystals are also shown in Fig. 11.

3) Platelike growth. Under certain conditions growth proceeds rapidly along the basal plane so that bright and shiny plates develop, see Fig. 12.

A crystal which had developed a mirrorlike hexagonal face about $\frac{1}{8}$ in. across was studied by means of an X-ray spectrometer. It was found that the bright surface was the basal plane. Such a crystal, etched to show its structure, is shown in Fig. 13.

Zirconium

The same typical outward crystal forms have been noted in zirconium electrodeposits as are seen in titanium. Again, as in the case of both zinc and titanium, the bright shiny deposits are those with strong basal plane development, as shown in Fig. 14.

Conclusions

The main conclusion that can be drawn from this study is that in electrodeposits of zinc, titanium, and zirconium a strong tendency exists for development of hexagonal faces which are actually the basal planes of the hexagonal-close-packed lattice. When this development is pronounced, the deposits are very bright and mirrorlike and appear brilliant to the eye.

Discussion of this paper sent (2 copies) to AIME by Nov. 1, 1956, will appear in *AIME Transactions* Vol. 209, 1957, and in JOURNAL OF METALS, October 1957.

Oxygen in Liquid Iron-Nickel Alloys

Equilibrium in the reaction of hydrogen gas with oxygen in liquid nickel, iron, and their alloys has been studied at temperatures of 1500° to 1700°C. The equilibrium constant, p_{H_2O}/p_{H_2} [% O], is greater in nickel than in iron by a factor of 45 at 1594°C. In the alloy steel range, the activity coefficient of oxygen is slightly increased by the presence of nickel.

by Henry A. Wriedt and John Chipman

NICKEL is more resistant to oxidation than is iron, in both the solid and the liquid state. The solubility of oxygen,[1] however, is greater in nickel than in iron. The two metals and their liquid alloys constitute an interesting series of solvents in which the chemical behavior of oxygen can be studied by methods similar to those used for liquid iron.[2]

Experimental Method

The furnace assembly, including the tube for preheating the entrant gas, was the same as that described by Gokcen and Chipman.[3] A controlled mixture of water vapor, hydrogen, and argon preheated to approximately the melt temperature impinged upon the surface of the liquid metal. The charge, weighing about 35 g, was held in an alumina crucible at constant temperature in the gas stream for a prolonged period, then quenched in a stream of cold helium, sampled, and analyzed for oxygen by the vacuum fusion method.

The temperature of the liquid metal was measured by a Leeds and Northrup optical pyrometer which had been checked against a similar instrument certified by the National Bureau of Standards. The freezing point of electrolytic iron in hydrogen, taken as 1535°C, was used for frequent checks. This point and the emissivity determined by Dastur and Gokcen[4] established the transmissivity of the optical system. The emissivities of Fe-Ni alloys at 1535° are known from the experiments of Smith and Chipman,[5] and it was assumed that the variation with temperature is parallel with that of iron. Recorded temperatures are probably accurate within ±5°C.

Preparation of Gas Mixtures—Since the ratio $H_2O:H_2$ had to be much higher for nickel than for iron, it was found convenient to construct two separate systems for gas preparation. In both cases, the ratio of argon to the sum of the other two gases was at least 4, the proportion of hydrogen and water vapor being varied to produce $H_2O:H_2$ ratios from 0.2 to 70. The primary purpose of the argon was to diminish the errors caused by thermal separation[3] in the gas phase. In addition, its presence lowered the saturation temperature required for a given $H_2O:H_2$ ratio and minimized the difficulty of preventing condensation. Both systems were modifications of the one used by Dastur and Chipman.[4]

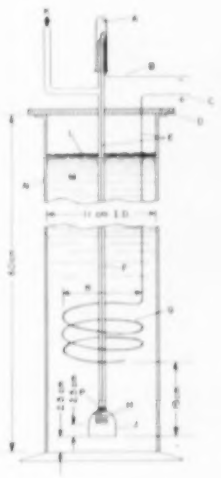


Fig. 1—Electrolytic cell: A, seal (De Khotinsky cement); B, cathode lead; C, anode lead; D, Lucite cover; E, nickel wire; F, 8 mm Pyrex tubing; G, anode; H, cathode; J, bell (25 mm Pyrex tubing); K, hydrogen; L, electrolyte level; M, 25 pct sodium hydroxide solution; N, Pyrex glass cylinder (60 cm high, 11 cm ID); P, electrolyte level inside cathode compartment; and R, 10 cm diam helix (3 turns).

For $H_2O:H_2$ ratios greater than 2, welding-grade argon purified by passage over magnesium chips at 620°C was mixed with hydrogen produced in the electrolytic cell shown in Fig. 1. The design was based on a cell used by Bodenstein and Pohl,[6] the important features of which are a nickel wire cathode and sodium hydroxide solution as the electrolyte. The rate of hydrogen evolution was controlled by a calibrated ammeter and a rheostat in the circuit, the current efficiency being taken as 100 pct. Precise control of the argon flow rate at about 300 ml per min was achieved by means of a capillary flowmeter having a controlled pressure drop. The molar flow rate was measured volumetrically before each run, and a constant flowmeter setting was maintained thereafter. The A-H_2 mixture passed through a water saturator of the type previously described,[4] the saturation temperature being maintained automatically with a fluctuation of ±0.04°C. The tube between saturator and furnace was heated by a resistance winding. The system was of all Pyrex construction with the exception of the stainless steel tube containing magnesium chips. This was connected with the glass tubing at the upstream end by

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TP 4280CE. Manuscript, Jan. 25, 1956. Cleveland Meeting, October 1956.

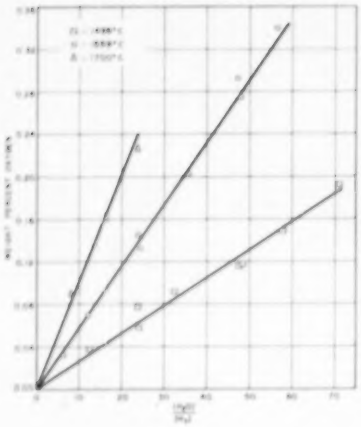

Fig. 2—Variation of oxygen content with $(H_2O)/(H_2)$ for pure nickel.

a rubber stopper coated with Glyptal and at the downstream end by a metal-to-glass seal.

For $H_2O:H_2$ ratios less than 2, the conventional saturator system[5] was employed. Instead of pure hydrogen, a prepared mixture containing 22.68 pct H_2, the balance argon, was passed into the saturator. This mixture was prepared in advance in a gas cylinder from pure hydrogen and welding-grade argon, and its composition was accurately determined by analysis. The flow rate of the gas mixture was approximately 300 ml per min, and a similar amount of purified argon was added through a second line to the saturated gas.

All components of the gas systems were brought to their operational conditions sufficiently early to ensure stabilization and the thorough purging of gas lines before the commencement of a run.

Procedure—The raw materials for the standard 35 g charges were electrolytic iron, electrolytic nickel, chemically pure nickel oxide, and iron oxide. Nickel oxide was used for charging oxygen to iron-free melts; otherwise, iron oxide was used.

A series of preliminary runs was made to determine the oxygen recovery in melting charges composed of nickel oxide and nickel. In runs with iron alloys, the oxygen recovery was estimated from the difference between the amounts of iron charged and recovered, counting iron lost as wüstite absorbed by the alumina crucible. These recovery estimates facilitated the charging of amounts of oxygen close to final values, thereby permitting short run times and, further, allowed the direction of approach to equilibrium to be determined. The variations in iron recovery resulted in slight differences in the final iron contents of alloys of the same nominal composition.

The preheater was brought to operating temperature slowly to prevent cracking while the gas system was being put in operation. In running, the inside surface of the preheater was maintained at the apparent temperature of the molten charge, except for the runs at 1500° when the apparent temperature was as for runs at 1600°C. This preheating and the mixture of a high proportion of argon with the reactive components of the gas are effective in reducing thermal diffusion to a minor effect in this type of system.[7]

The charge was heated with power supplied by the high frequency converter. The temperature of the melt was controlled manually, and heats where temperature fluctuation exceeded ±3° during the last several hours of a run were rejected. Each run was concluded by pulling the charge quickly to the bottom of the furnace chamber and quenching it with a blast of cold helium.

The duration of each heat was predetermined from the experience gained in preceding equilibrium or nonequilibrium runs. Charges were so calculated that the reaction between gas and melt took less than 9 to 10 hr in reducing the final deviation from the equilibrium composition to a small fraction of the total experimental error. In those series of heats where equilibrium was not approached from both sides, heat times were ample according to the findings in other series.

Sampling and Analysis—The quenched charge was first freed from adhering refractory. From the disk-shaped alloy slug, wedge-like samples were taken by radial sawcuts. These were ground and pickled before analyzing. Nickel was determined in the iron alloys and the 75 pct Fe content obtained by difference. In all other alloys, iron was determined directly. The vacuum fusion method was used for all oxygen analyses.

Experimental Results

The experimental results are summarized in Tables I, II, III, and IV. The first three contain the

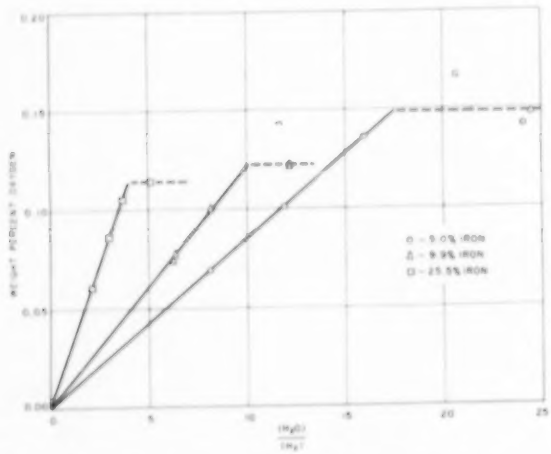

Fig. 3—Variation of oxygen content with $(H_2O)/(H_2)$ at 1594°C.

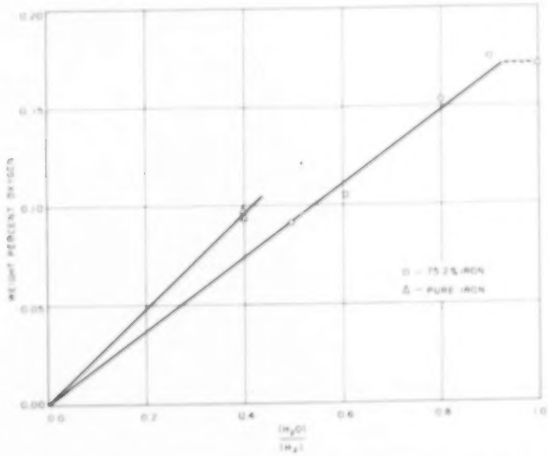

Fig. 4—Variation of oxygen content with $(H_2O)/(H_2)$ at 1594°C.

data for equilibrium runs with Ni-O, Fe-Ni-O, and Fe-O alloys, respectively, while Table IV contains the data for nonequilibrium heats. These latter were made with gas ratios in excess of those for oxygen saturation of the alloys concerned. The tables have been subdivided into heat series within each of

Table I. Equilibrium Data for Ni-O Alloys

Heat No.	Probable Initial Pct Oxygen	Duration, Hr	Corrected p_{H_2O}/p_{H_2}	Pct Analyzed Oxygen	K_1' (0)
Series I: Pure Nickel, 1599°C					
212	0.0	4.8	6.37	0.032	199
213	0.0	7.6	12.3	0.070	176
242	0.17	8.5	23.7	0.145	163
214	0.13	8.8	24.3	0.133	182
245	0.22	8.5	35.5	0.203	175
243	0.31	8.7	47.0	0.293	160
221	0.08	8.2	47.6	0.275	173
244	0.34	7.3	56.2	0.340	165
Series II: Pure Nickel, 1700°C					
261	0.0	5.4	8.16	0.090	90.7
262	0.021	8.1	16.3	0.161	101
263	0.30	9.2	23.7	0.227	104
Series III: Pure Nickel, 1496°C					
240	0.0	6.0	12.5	0.038	329
230	0.0	8.0	23.8	0.077	309
231	0.07	8.4	24.0	0.058	414
241	0.10	4.2	32.3	0.092	351
233	0.12	8.3	47.8	0.116	413
232	0.0	8.3	48.1	0.117	411
250	0.18	7.9	57.6	0.148	389
249	0.23	8.5	70.9	0.192	369

which the ratio of iron to nickel and the temperature are constant. For each experimental run, the mass action *constant* K_1' (X) is tabulated for the reaction

$$H_2(\text{gas}) + O(\text{dissolved in } X \text{ pct Fe alloy}) = H_2O(\text{gas})$$

$$K_1'(X) = \frac{p_{H_2O}}{p_{H_2}[\%\ O]}. \qquad [1]$$

The true equilibrium constant K_1 (X) is given by

$$K_1(X) = \frac{p_{H_2O}}{p_{H_2}[a_O]}.$$

The standard state of each gaseous component mentioned in this and succeeding sections is defined

Table II. Equilibrium Data for Fe-Ni-O Alloys

Heat No.	Probable Initial Pct Oxygen	Duration, Hr	Corrected p_{H_2O}/p_{H_2}	Pct Oxygen	Analyzed Iron	$K_1'(X)$
Series IV: Ni-5.0 Pct Fe, 1594°C						
247	0.0	5.9	8.12	0.069	5.14	118
246	0.0	8.0	11.9	0.101	4.97	118
248	0.0	8.2	16.0	0.136	5.00	118
Series V: Ni-9.9 Pct Fe, 1594°C						
253	0.10	6.9	6.22	0.074	9.88	84.0
254	0.0	5.8	6.31	0.077	10.12	82.0
252	†	8.8	8.19	0.100	9.66	81.9
Series VI: Ni-25.5 Pct Fe, 1594°C						
259	0.08	4.0	2.15	0.061	25.60	35.2
258	0.08	4.0	3.02	0.086	25.35	35.1
257	†	4.2	3.74	0.105	25.50	35.6
Series VII: Ni-25.5 Pct Fe, 1650°C						
276	0.02	5.5	2.93	0.119	25.15	24.6
Series VIII: Ni-25.5 Pct Fe, 1500°C						
274	0.0	3.8	2.94	0.047	26.00	62.6
Series IX: Ni-75.2 Pct Fe, 1594°C						
281	0.16	2.7	0.496	0.091	75.27*	5.46
280	0.14	2.8	0.606	0.105	75.20*	5.77
279	0.14	2.7	0.802	0.153	75.18*	5.25
278	0.12	2.5	0.900	0.175	75.10*	5.14

* By difference.
† Saturation point.

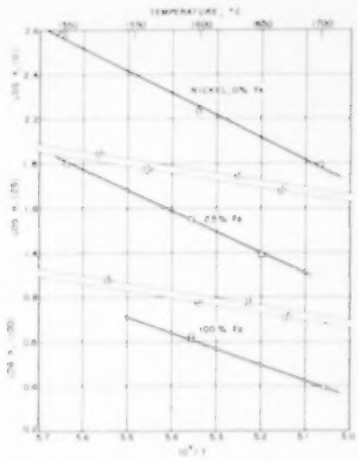

Fig. 5—Observed values of log p_{H_2O}/p_{H_2} [% O]; lowest line from Dastur and Chipman.

as the pure component at 1 atm pressure. For each Fe-Ni alloy, the standard state of dissolved oxygen is chosen so that the ratio $a_O/[\%\ O]$ approaches unity as [% O] approaches zero.

Oxygen Activity in Liquid Fe-Ni-O Alloys—For pure Ni-O alloys, the average K_1' (0) values are as follows: 381 at 1496° (series III omitting heat 230), 171 at 1599° (series I omitting heat 212), and 98.7 at 1700°C (series II). Lines based upon these average K_1' (0) values are shown with the experimental data in Fig. 2. Up to the maximum oxygen contents investigated in the three series, the straight lines fit the data within experimental error. Therefore, activity of oxygen may be equated with percentage of oxygen throughout each experimental range, and K_1' (0) becomes K_1 (0). Since the same behavior has been reported in Fe-O alloys, the primes may be dropped, and the activity of oxygen is replaced by its weight percentage in each alloy.

For alloys containing both iron and nickel at 1594°C, the average values of K_1 (X) are: 118 [K_1 (5)] at 5.0 pct Fe (series IV), 82.6 [K_1 (10)] at 9.9 pct Fe (series V), 35.3 [K_1 (25)] at 25.5 pct Fe (series VI), and 5.41 [K_1 (75)] at 75.2 pct Fe (series IX). Straight lines calculated from these average K_1 (X) values are shown with the experimental points in Figs. 3 and 4. The straight lines are terminated in each case at oxygen contents indicated by the nonequilibrium data of Table IV to be the saturation values in alumina crucibles. Two

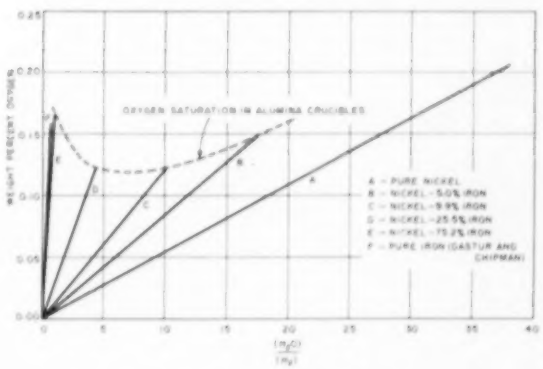
OXYGEN SATURATION IN ALUMINA CRUCIBLES
A - PURE NICKEL
B - NICKEL - 5.0% IRON
C - NICKEL - 9.9% IRON
D - NICKEL - 25.5% IRON
E - NICKEL - 75.2% IRON
F - PURE IRON (DASTUR AND CHIPMAN)

Fig. 6—Summary of the variation of oxygen content with $(H_2O)/(H_2)$ at 1594°C.

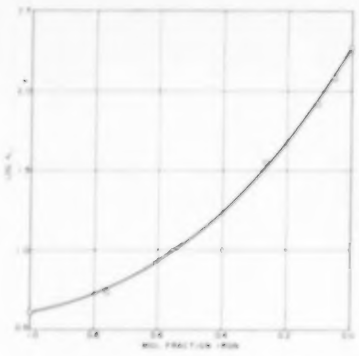

Fig. 7—Variation of log K_1 (X) with iron content of alloy at 1594°C.

additional runs were made with alloys containing 25.5 pct Fe; one at 1650° and one at 1500°C.

For pure Fe-O alloys, for which Fontana and Chipman[9] have established that the activity of oxygen is proportional to the concentration, K_1 (100) values were obtained at 1594° in two runs and at 1704°C in one run. The results at 1594°C are plotted in Fig. 4 together with a line given by the mean K_1 (100) value of the two experimental points.

Table III. Equilibrium Data for Fe-O Alloys

Heat No.	Probable Initial Pct Oxygen	Duration, Hr	Corrected p_{H_2O}/p_{H_2}	Pct Analyzed Oxygen	K'_1 (100)
Series X: Pure Iron, 1594°C					
282	0.15	2.5	0.398	0.097	4.10
283*	0.15	2.5	0.390	0.094	4.25
Series XI: Pure Iron, 1704°C					
284	0.27	5.1	0.236	0.095	2.49

* MgO crucible.

Variation of K_1 (X) with Temperature—From the foregoing data, the variations of K_1 (X) with temperature may be obtained for liquid alloys of oxygen with pure nickel, with Ni-25.5 pct Fe, and with pure iron. These are shown in Fig. 5.

Equations for the three lines and the corresponding free-energy changes are:

For pure nickel

$$\log K_1\,(0) = +10{,}040/T - 3.11$$
$$\Delta F_1^\circ\,(0) = -45{,}900 + 14.2T.$$

For 25 pct Fe

$$\log K_1\,(25) = +9{,}250/T - 3.41$$
$$\Delta F_1^\circ\,(25) = -42{,}300 + 15.6T.$$

For pure iron, the three observations confirm the results of Dastur and Chipman, whose equation is

$$\log K_1\,(100) = +7{,}050/T - 3.17$$
$$\Delta F_1^\circ\,(100) = -32{,}250 + 14.50T.$$

Fig. 6 is a summary of the relationships between oxygen content and water vapor to hydrogen ratio at 1594°C for all the alloys involved in this study. The lines for pure Ni-O and pure Fe-O alloys were drawn, using K_1 (0) and K_1 (100) values of 184 and 4.05, respectively, derived from Fig. 5. The line for each Fe-Ni-O alloy is based upon the average of the direct experimental K_1 (X) values for that alloy at 1594°C. The line of oxygen saturation in alumina crucibles is taken from the smoothed results published previously.[1]

Standard Free Energy of Solution of Oxygen—The standard free energy change, ΔF_2°, for the reaction

$\frac{1}{2}$ O_2 (gas) = $\underline{O}$ (dissolved in liquid X pct Fe alloy)

$$K_2\,(X) = a_O/p_{O_2}^{1/2} \qquad [2]$$

can be derived from the foregoing for pure Ni-O alloys and for alloys containing 25.5 pct Fe.

For the reaction

$$H_2\ (\text{gas}) + \tfrac{1}{2}\,O_2\ (\text{gas}) = H_2O\ (\text{gas}) \qquad [3]$$

the standard free energy change at steelmaking temperatures is given[9] as

$$\Delta F_3^\circ = -60{,}180 + 13.93T.$$

Combining the expression for ΔF_3° with those for ΔF_1°

$$\Delta F_2^\circ\,(0) = -14{,}300 - 0.3T$$
$$\Delta F_2^\circ\,(25) = -17{,}900 - 1.7T$$
$$\Delta F_2^\circ\,(100) = -27{,}930 - 0.5T$$

is obtained. These equations show a pronounced effect of composition on the heat of the reaction. At the same time, it is noted that the entropy terms are nearly equal.

Variation of K_1 (X) at 1594°C with Alloy Composition—In Fig. 7, log K_1 (X) at 1594°C is shown as a function of the mol fraction of iron. The datum points represent the identical K_1 (X) values upon which the lines of Fig. 6 were based.

The curve in Fig. 7 demonstrates that K_1 (X) is a sensitive function of alloy composition, particularly at high nickel contents. In going from nickel-free alloys to iron-free alloys, K_1 (X) increases by a factor of 45.4. This is in marked contrast to the results of a similar study of sulfur in the liquid alloys[10] in which the corresponding equilibrium constant was found to be independent of the Fe-Ni ratio.

Reaction of Pure Alumina with Liquid Alloys—At the conclusion of each run, the alumina crucible was examined for signs of reaction with the liquid alloy. Where reaction had occurred, the product formed only a superficial layer on the crucible and it seems unlikely that the reaction affected the results of the equilibria studied. For solutions in pure nickel, the oxygen content at which a reaction product began to be evident was between 0.038 and 0.058 at 1496° and between 0.090 and 0.161 at 1700°C. In the iron-bearing alloys the oxygen content was always sufficient to blacken the crucible. In only one instance, heat No. 284, a crucible containing pure iron with 0.095 pct O at 1704°C came out white.

Table IV. Nonequilibrium Data for Runs in Alumina Crucibles

Heat No.	Duration, Hr	Corrected p_{H_2O}/p_{H_2}	Pct Analyzed	
			Oxygen	Iron
Series IVB: Ni-5.0 Pct Fe, 1594°C				
251	5.6	20.7	0.167	4.81
255	1.2	24.1	0.142	4.87
237	8.4	24.6	0.148	4.83
Series VB: Ni-9.9 Pct Fe, 1594°C				
256	1.8	12.2	0.122	10.07
Series VIB: Ni-25.5 Pct Fe, 1594°C				
260	0.1	5.11	0.114	25.60
Series IXB: Ni-75.2 Pct Fe, 1594°C				
277	2.6	0.997	0.171	74.92

Application to Alloy Steelmaking

In the foregoing treatment, the activity of oxygen in each alloy was defined by letting the ratio $a_o/[\%\ O]$ approach unity as $[\%\ O]$ approaches zero. It was shown that within the limits of error of the experiments this was equivalent to taking the activity of oxygen as equal to its percentage *in each alloy*.

On the other hand, for general steelmaking purposes it is convenient and conventional to define the activity of oxygen in terms of a reference state based on the dilute solution of oxygen in pure iron. The effect of an alloying element is then expressed as a factor in the activity coefficient of oxygen. Thus, for a given Fe-Ni-O alloy

$$\log f_o = \log f_o' + \log f_o^{(Ni)}.$$

Here f_o' is the activity coefficient of oxygen in a pure Fe-O solution of the given oxygen concentration, and $f_o^{(Ni)}$ represents the effect of the nickel present. It is known[6] that f_o' is very close to unity and hence that $\log f_o'$ is negligible. Values of $\log f_o^{(Ni)}$ may be obtained for any nickel concentration by using the data plotted in Fig. 7.

The equilibrium constant of the reaction studied may be rewritten in the form

$$K_1^{(Fe)} = \frac{p_{H_2O}}{p_{H_2} f_o [\%\ O]}$$

from which by comparison with Eq. 1

$$\log f_o = \log K_1' (X) - \log K_1 (Fe).$$

Values of $\log K_1' (X)$ are shown in Fig. 7, and $\log K_1 (Fe)$ is the limiting value at 100 pct Fe. A plot of $\log f_o$ which is the same as $\log f_o^{(Ni)}$ is, therefore, parallel to the curve of Fig. 7 with its zero at the pure iron end of the curve.

Within the usual range of alloy steel compositions, approximately 0 to 25 pct Ni, the effect of nickel on the activity coefficient of oxygen is given approximately as

$$\log f_o^{(Ni)} = 0.006\ [\%\ Ni].$$

Acknowledgments

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Discussion of this paper sent (2 copies) to AIME by Nov. 1, 1956 will appear in AIME *Transactions* Vol. 209, 1957, and in JOURNAL OF METALS, October 1957.

Technical Note

Exposure of Shrinkage Cavity during the Reheating of Open Top Killed Steel Ingots

by L. J. Trilli

RECENT studies have indicated that there may be considerable difference in the cleanliness of the central pipe cavity of open top ingots. It has been speculated that some of this difference arises because of reactions that occur during reheating in the blooming mill soaking pits. In this respect, it has been recognized that an oxide coating or flow of slaggy material in the cavity could inhibit welding during hot rolling and thus reduce the amount of sound product which may be obtained. This note is intended to summarize some observations on the structure of a split ingot top which may be useful in describing a mechanism whereby the shrinkage cavity could be oxidized during reheating.

A 22½x24½ in., big-end-down, test ingot was secured from a routine production heat. The chemical analysis representing this ingot is as follows: 0.08 pct C, 1.04 pct Mn, 0.062 pct P, 0.315 pct S, and 0.02 pct Si. The deoxidation practice used on the heat from which the experimental ingot was

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Fig. 1—Deep etched surface of split ingot top after heating in the soaking pits.

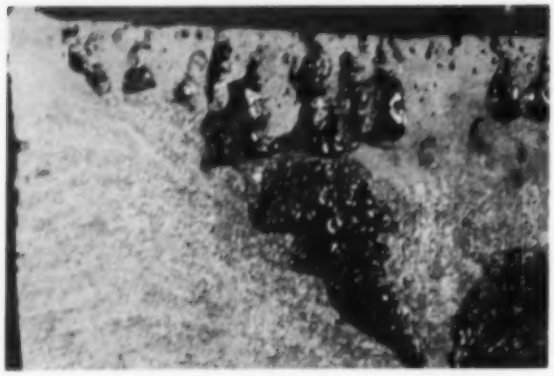

Fig. 2—Upper left hand corner of split ingot top at X2. Area reduced approximately 50 pct for reproduction.

obtained consisted of a bath addition of spiegel, serving primarily as a carbon block. Ladle additions of regular 80 pct ferromanganese and elemental sulfur were used to obtain the desired final analysis. The heat was delivered to the blooming mill and charged into the soaking pits in a routine manner. Following a normal heating period, the ingot was removed from the pit and permitted to air cool instead of rolling on the blooming mill. The top 18 in. of the ingot, equivalent to 23 pct of total height, was cut off and sectioned parallel to the vertical axis. Photographs of these sections, which were deep etched, are shown in Figs. 1 and 2.

In most respects, the internal structure of the top of this ingot shows little difference from the usual conditions observed for normal open top steels of this type. As will be seen from the photograph of the deep etched surface, the shrinkage cavity has curved sides with one continuous bridge located near the mid-position. Evidence of several other bridges which presumably collapsed during freezing is apparent from the projections along the sides. Although not visible in the photographs, the crown-like opening atop the cavity expands to within 1 in. of the top surface and 3 in. of the ingot sides. The vertical lines faintly visible adjacent to the lower half of the cavity appear to be lines of negative segregation.

One notable feature in the sectioned ingot is the great number of exposed blowholes located in the top crust. From the top surface, they appear as holes varying in size from pinholes to ¼ in. in diam. Extensive probing with a wire indicated that, while the holes were predominantly surface indentations, many had depths ranging from ½ to 2 in. In addition, others led directly into the pipe cavity.

Fig. 2 is a photograph of the upper left corner of the ingot expanded to X2. It illustrates more clearly that the blowholes had their tops opened, and also illustrates the interconnection among the blowholes at a short distance below the surface. In addition to the direct entries leading into the cavity, this honeycomb structure offers additional paths. The limit of penetration would be the continuous bridge that formed during solidification. The heavy slagging which accompanied the torch cutting of the ingot made it impossible to determine the actual presence of any coating in the cavity which could be related to the heating process.

The structure of another sectioned ingot, which was not heated in the soaking pits, exhibited blowholes in the top crust also. However, they were seated slightly below the top surface and were covered by a thin layer of metal. This condition, shown in Fig. 3, is the usual pattern observed in the unheated steel. The presence of the blowholes may be attributed to the marginal deoxidation practice, as well as the increased oxygen pressure and decreased ferrostatic head near the ingot top. In a comparison of these two ingot structures, it appears that a combination of scaling and impinging flame action has removed some of the metal layer composing the top ingot top surface. As a result, a number of the blowholes were exposed to the pit atmosphere and could provide entries into the shrinkage cavity for oxidizing pit gases. Whether or not a hole is opened apparently depends upon the thickness of the steel over it and the severity of flame action.

While this discussion has shown how the shrinkage cavity may be contaminated during the ingot reheating operation, mention should be made of several factors which control the severity of its occurrence. The ingot studied here was heated in a relatively shallow pit with a strong flame action sweeping directly across the tops. The combined effect of these pit conditions would probably be less in pits of different design. The presence of continuous bridges in the cavity would limit the extent of contamination. However, the location and number of bridges formed during freezing cannot be predicted, and are unique to steelmaking variables for each ingot.

This note has described the somewhat porous structure which exists immediately below the top ingot surface of certain open top steels. The characteristic porosity is caused by the development of blowholes during solidification in the crust over the shrinkage cavity. From the examination of a section of an ingot which was heated in the soaking pits, it may be tentatively concluded that some of the thin metal layer composing the top ingot surface is removed through the combination of scaling and impinging flame action. As a result, a number of the blowholes could be exposed and thus provide channels, both direct and by interconnection, for the passage of oxidizing gases into the shrinkage cavity.

Fig. 3—Sulfur print of an ingot top not heated in the soaking pits. Area reduced approximately 50 pct for reproduction.

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AIME NEWS

IMD Fall Meeting — Complete Program

Hotel Carter, Cleveland — October 8—10, 1956

This year the Fall Meeting of the Institute of Metals Division will be held at Hotel Carter in Cleveland, October 8-10. Symposiums of special interest have been planned on nuclear metallurgy, titanium, and magnetism. The Symposium on Nuclear Metallurgy will be held on Monday, with three papers describing the irradiation effects on metals.

The Fifth Annual Titanium Symposium will be held Tuesday, October 9. The morning session will feature panel discussions on the economics of sponge and mill production of titanium, and of fabrication of titanium. In the afternoon, panel discussions will be conducted on the *State of Analytical Development* and *Practical Problems Associated with the Control of Interstitials.*

The Symposium on Magnetism has been arranged by the IMD Metal Physics Committee, and will present papers on the *Fundamentals of Magnetism, Kinetics of Magnetization,* and *Metallurgy of Magnetic Materials.* Also scheduled for Wednesday is a Powder Metallurgy Luncheon in the Embassy Room of Hotel Carter, followed by a panel discussion.

Registration for the meeting will start Sunday at 3 pm in the Mezzanine of Hotel Carter. Monday registration will be from 8 am to 5 pm; Tuesday and Wednesday from 9 am to 5 pm. Registration is $2.00 for members and $4.00 for nonmembers. AIME Student Associates will not be charged for registration; non-AIME students will pay $1.00.

At the Annual IMD Fall Dinner in the Grand Ballroom at 7 pm, Frederick C. Crawford, chairman of the board, Thompson Products, Inc. will be the principal speaker.

IMD's Fall Meeting is part of the Annual National Metal Congress. The American Society for Metals, with headquarters at the Statler Hotel, will hold a special seminar on Saturday and Sunday, October 6-7, on the subject of *Creep and Recovery.* A one-day Conference on Thorium Metal is scheduled for Thursday, October 11, jointly sponsored by ASM and the Atomic Energy Commission. The ASM Campbell Memorial Lecturer will be Charles S. Barrett, Professor, Institute for Study of Metals, University of Chicago, who will speak at 10 am Wed.

The American Welding Society will have its headquarters at the Cleveland Hotel where papers covering practically all phases of welding activity will be presented at 19 technical sessions.

The National Metal Exposition is being held at the Public Auditorium and Exhibition Halls in Cleveland during the week of October 8 to 12.

SUNDAY, OCTOBER 7

3:00 to 9:00 pm, Mezzanine

Registration

8:00 pm, Aviation Room

IMD Publications Committee Meeting

P. A. Beck, Chairman

MONDAY, OCTOBER 8

8:00 am to 5:00 pm, Mezzanine

Registration

9:00 am to 12:00 m, Petit Cafe

Research in Progress

B. D. Cullity and Paul Gordon, Chairmen

Abstracts of papers will not be published but will be available in mimeographed form at the registration desk during the meeting.

9:00 am to 12:00 m, Grand Ballroom

Research Seminar — Embrittlement

W. R. Hibbard, Jr. and J. P. Nielsen, Chairmen

Effect of Hydrogen on Alpha Titanium Alloys: G. A. Lenning, Titanium Metals Corp. of America, J. W. Spretnak, Ohio State University, and R. I. Jaffee, Battelle Memorial Institute.*

Upper-Nose Temper Embrittlement of a Nickel-Chromium Steel: L. D. Jaffe and D. C. Buffum, Jet Propulsion Laboratory, California Institute of Technology.*

On the Nature of Embrittlement Occurring While Tempering a Ni-Cr Alloy Steel: G. Bhat, Crucible Steel Co. of America, and J. F. Libsch, Lehigh University.*

Observations on the Mechanical Properties of Hydrogenated Vanadium: B. W. Roberts and H. C. Rogers, General Electric Co.*

Effect of Small Amounts of Alloying Elements on the Ductility of Cast Molybdenum: L. E. Olds and G. W. P. Rengstorff, Battelle Memorial Institute.

* Published in JOURNAL OF METALS, October 1956.

(Program continued on page 1202)

12 m, Aviation Room

Titanium Committee Luncheon Meeting

D. J. McPherson, Chairman

2:00 to 5:00 pm, Grand Ballroom

Symposium on Nuclear Metallurgy

Edward Epremian and A. E. Kaufmann, Chairmen

Theory and Mechanism of Radiation Effects in Metals: G. H. Vineyard, Brookhaven National Laboratory.

Irradiation Effects On Physical Metallurgical Processes: D. E. Thomas, Westinghouse Electric Corp.

Irradiation Effects in Reactor Materials: D. S. Billington, Oak Ridge National Laboratory.

2:00 to 5:00 pm, Petit Cafe

Research Summaries — Mechanical Properties

W. J. Harris, Jr. and R. W. Fountain, Chairmen

Influence of Silicon and Aluminum on the Properties of Hot-Rolled Steels: R. H. Frazier, F. W. Boulger, and C. H. Lorig, Battelle Memorial Institute.*

Creep of Polycrystalline Nickel: J. Weertman and P. Shahinian, Naval Research Laboratory.*

Study of the Effect of Gases on the Melting, Casting, and Working of Palladium: R. H. Atkinson, International Nickel Co. Inc. (retired). JOURNAL OF METALS, August 1956.

Effect of Molybdenum, Tungsten, and Vanadium on the High-Temperature Rupture Strength of Ferritic Steel: A. E. Powers, General Electric Co.*

Titanium-36 Pct Al as a Base for High Temperature Alloys: J. B. McAndrew, Armour Research Foundation, and H. D. Kessler, Titanium Metals Corp. of America.*

High-Strength Zirconium Alloy: Zirconium + 4 Wt Pct Sn + 1.6 Wt Pct Mo: Walston Chubb, Battelle Memorial Institute.

Creep of Single Crystals and Polycrystals of Aluminum, Lead, and Tin: C. D. Wiseman, O. D. Sherby, and J. E. Dorn, University of California.

Effect of Melting and Casting Atmospheres on the Stress-Rupture Properties of Cast Nickel-Base Alloys: C. M. Hammond and R. A. Flinn, University of Michigan.*

Aging in Complex Commercial Ni-Cr Alloys Hardened With Titanium and Aluminum: R. F. Wilde, General Motors Corp. and N. J. Grant, MIT.

Effect of Orientation on the Plastic Deformation of Aluminum Single Crystals and Bicrystals: R. S. Davis, R. L. Fleisher, J. D. Livingston, and Bruce Chalmers, Harvard University.

4:30 pm, Aviation Room

IMD Membership Committee Meeting

Harold Margolin, Chairman

7:30 pm, Aviation Room

IMD Programs Committee Meeting

M. E. Nicholson, Chairman

TUESDAY, OCTOBER 9

9:00 am to 5:00 pm, Mezzanine

Registration

9:00 am to 12:00 m, Petit Cafe

Research Seminar — Crystalline Imperfections and Mechanical Behavior

Jack Washburn and Robert Maddin, Chairmen

Delayed Yielding in a Substitutional Solid Solution Alloy: L. A. Shepard and J. E. Dorn, University of California.*

Control of Strain Aging in Alpha Iron: E. R. Morgan, Jones & Laughlin Steel Corp. and J. C. Shyne, Ford Motor Co.

Recovery of Cold-Worked High-Purity Al-Mg Alloys: E. C. W. Perryman, Atomic Energy of Canada, Ltd.*

Temperature Dependence of Annealing Phenomena in a Cold Rolled Aluminum Single Crystal: A. H. Lutts and P. A. Beck, University of Illinois.*

Investigation of the Effect of Solutes on the Grain Boundary Stress Relaxation Phenomenon: S. Weinig, New York University, and E. S. Machlin, Columbia University.*

Some Observations on the Structure of Grain Boundary Fracture Surfaces: H. C. Chang and N. J. Grant, Massachusetts Institute of Technology.

9:00 am to 12:00 m, Grand Ballroom

Symposium on Titanium

B. S. Mesick and D. J. McPherson, Chairmen

R. H. Thielemann, Symposium Moderator

I. Panel Discussion: *Sponge and Mill Production Economics*

Speaker:
H. H. Kellogg, Columbia University

Panelists:
W. E. Lusby, Jr., E. I. du Pont de Nemours & Co.
T. W. Lippert, Titanium Metals Corp. of America
P. Alspaugh, Electro Metallurgical Co.
V. W. Whitmer, Republic Steel Corp.
D. W. Kaufmann, Rem-Cru Titanium Inc.
F. Vandenburgh, Mallory-Sharon Titanium Corp.

II. Panel Discussion: *Fabrication Economics*

Speaker:
S. A. Gordon, Battelle Memorial Institute

Panelists:
G. Fairbairn, North American Aviation Inc.
W. H. Sharp, Pratt and Whitney Aircraft Div., United Aircraft Corp.
A. L. Rustay, Wyman-Gordon Co.
R. A. Paetz, Thompson Products Inc.
S. R. Carpenter, Convair Div., General Dynamics Corp.

12:00 m, Aviation Room

IMD Executive Committee Luncheon Meeting

C. S. Barrett, Chairman

2:00 to 5:00 pm, Grand Ballroom

Symposium on Titanium

B. S. Mesick and D. J. McPherson, Chairmen

R. H. Thielemann, Symposium Moderator

III. Panel Discussion: *State of Analytical Development*

Speaker:
S. Vigo, Watertown Arsenal

Panelists:
T. D. McKinley, E. I. du Pont de Nemours & Co.
J. E. Griffin, Mallory-Sharon Titanium Corp.
H. T. Clark, Jr., Rem-Cru Titanium Inc.
A. F. Ernster, Ford Motor Co.
W. A. Dupraw, National Research Corp.
M. J. Miles, Titanium Metals Corp. of America

IV. Panel Discussion: *Practical Problems Associated With the Control of Interstitials*

Speakers:
L. S. Busch, Mallory-Sharon Titanium Corp.
H. Brown, Solar Aircraft Co.
C. Handova, North American Aviation Inc.

Panelists:
H. T. Clark, Jr., Rem-Cru Titanium Inc.
T. Perry, Republic Steel Corp.
S. A. Herres, Titanium Metals Corp. of America
J. A. King, Thompson Products of Canada

2:00 to 5:00 pm, Petit Cafe

Research Summaries — Atom Movements and Structure

F. J. Shortsleeve and D. J. Blickwede, Chairmen

Basal Plane Development in Electrodeposited Hexagonal Close-Packed Metals—Zinc, Titanium, Zirconium: W. R. Opie, National Lead Co. JOURNAL OF METALS, Sept. 1956.

Surface Activity Methods for the Determination of Diffusion Coefficients in Solids: R. H. Condit and C. E. Birchenall, James Forrestal Research Center, Princeton University.*

X-Ray Diffraction Determination of the Coefficients of Expansion of Alpha-Uranium: J. R. Bridge, C. M. Schwartz, and D. A. Vaughan, Battelle Memorial Institute.*

Solubility of Boron in Fe_3C and Variation of Saturation Magnetization, Curie Temperature, and Lattice Parameter of $Fe_3(C, B)$ with Composition: M. E. Nicholson, University of Minnesota.

Crystal Structure of Delta-Prime Plutonium and the Thermal Expansion Characteristics of Delta, Delta-Prime, and Epsilon Plutonium: F. H. Ellinger, Los Alamos Scientific Laboratory, University of California.*

Structure of the Transition Phase, Omega, in Ti-Cr Alloy: A. E. Austin and J. R. Doig, Battelle Memorial Institute.

Neutron Diffraction Study of Textures in Drawn-Body-Centered-Cubic Metals: R. A. Swalin, University of Minnesota, and A. H. Geisler (deceased).*

Relative Interfacial Energies of Symmetrical Tilt Grain Boundaries in Silver: K. T. Aust, General Electric Co. JOURNAL OF METALS, August 1956.

4:30 pm, Aviation Room

Nuclear Metallurgy Committee Meeting

Benjamin Lustman, Chairman

WEDNESDAY, OCTOBER 10

9:00 am to 5:00 pm, Mezzanine

Registration

9:00 to 10:30 am, Grand Ballroom

Research Summaries — Kinetics and Thermodynamics

C. E. Birchenall and R. A. Swalin, Chairmen

Structure Dependent Corrosion of Cu, Au—Experiments Relating to the Mechanism of Stress Cracking of Homogeneous Solid Solutions: Robert Bakish, Sprague Electric Co., and W. D. Robertson, Yale University.*

Growth of External Copper Layers During the Internal Oxidation of Dilute Cu-Al Alloys in a Cuprous Oxide-Copper Pack: D. L. Wood, General Electric Co.*

Free Energy of Formation of Mn_3C_2 From Vapor-Pressure Measurements: C. L. McCabe and R. G. Hudson, Carnegie Institute of Technology.

Oxygen in Liquid Iron-Nickel Alloys: H. A. Wriedt, U. S. Steel Corp., and John Chipman, MIT. JOURNAL OF METALS, Sept. 1956.

9:00 to 10:30 am, Petit Cafe

Research Summaries — Transformation

J. S. Smart, Jr. and R. F. Hehemann, Chairmen

Study of the Iron-Silicon Order-Disorder Transformation: F. W. Glaser, Alloy Precision Castings Co., and W. Ivanick, Schwarzkopf Development Corp.*

Structural Changes During the Aging in an Al-Mg-Zn Alloy: L. F. Mondolfo, Illinois Institute of Technology, N. A. Gjostein, Carnegie Institute of Technology, and D. W. Levinson, Armour Research Foundation.*

Sympathic Nucleation of Ferrite: H. I. Aaronson and C. Wells, Carnegie Institute of Technology.*

Electrical Resistivities and Phase Transformations of Lanthanum, Cerium, Praseodymium, and Neodymium: F. H. Spedding, A. H. Daane, and K. W. Herrmann, Iowa State College.

12:00 m, Embassy Room

Powder Metallurgy Luncheon

C. G. Johnson, Chairman

2:00 to 5:00 pm, Grand Ballroom

Symposium on Magnetism

J. C. Fisher and Morris Fine, Chairmen

Fundamentals of Magnetism: J. B. Goodenough, Lincoln Laboratory, MIT.

Kinetics of Magnetization: D. S. Rodbell, General Electric Co.

Metallurgy of Magnetic Materials: J. J. Becker, General Electric Co.

2:00 to 5:00 pm, Petit Cafe

Research Summaries — Alloy Systems

A. D. Schwope and William Rostoker, Chairmen

Study of Ferrous Ternary Diagrams in Relation to Magnetic Interactions: the FeNiAl System: U. H. Roesler, Westinghouse Research Laboratories.*

Melting Point Determination of Hafnium, Zirconium, and Titanium: D. K. Deardorff and E. T. Hayes, U. S. Bureau of Mines. JOURNAL OF METALS, May 1956.

Gamma Loop Studies in the Fe-Si and Fe-Si-Ti Systems: G. G. Bentle and W. P. Fishel, Vanderbilt University.*

Uranium-Bismuth System: R. J. Teitel, Brookhaven National Laboratory.

Uranium-Silicon Alloys: A. E. Kaufmann, Nuclear Metals, Inc., B. D. Cullity, University of Notre Dame, and G. Bitsianes, University of Minnesota.

Uranium-Zinc System: P. Chiotti, H. H. Klepfer, and K. J. Gill, Iowa State College.

Constitution of Nickel-Rich Quaternary Alloys of the Ni-Cr-Ti-Al System: A. Taylor, Westinghouse Research Laboratories.*

Solid Solubility of Carbon in Chromium: W. H. Smith, General Electric Co.

2:00 pm, Aviation Room

Powder Metallurgy Committee Meeting

C. G. Johnson, Chairman

AIME 1957 ANNUAL MEETING

Plans for the 1957 AIME Annual Meeting are progressing nicely. Scheduled for February 24-28, at the Roosevelt and Jung Hotels in New Orleans, the meeting will feature technical sessions and symposiums as well as luncheons and a banquet. Metals Branch headquarters will be at the Jung Hotel. Members who plan to attend should make arrangements early. For details and registration blank see page 1112A, August JOURNAL OF METALS.

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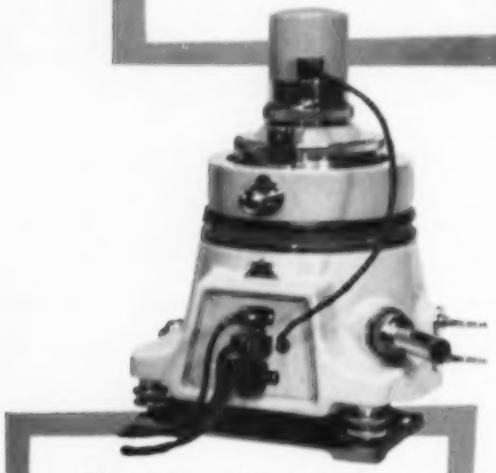

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Herbert Hoover, Jr. To Receive Hoover Medal

The Hoover Medal for 1956, one of the engineering profession's highest awards, will be presented to Under Secretary of State Herbert Hoover, Jr. Named after former President Hoover, father of the Under Secretary, and in 1930, its first recipient, is awarded each year by the medal sponsoring engineering societies, AIME, ASCE, ASME, and AIEE "to a fellow engineer for distinguished public service."

Scott Turner, chairman of the Hoover Medal Board of Award, announced the choice. The board consists of three representatives from each of the four sponsoring organizations. Dr. Turner is a Past President of AIME and a former director of the U. S. Bureau of Mines. Time and place of the presentation will be announced later. The ceremony will be under direct sponsorship of AIME, of which the winner has been a member since 1937. Former President Hoover, a member since 1896, is AIME Senior Past President, having held office in 1920. The Board of Directors in 1917 elected him an Honorary Member, highest AIME distinction and in 1928 he was the recipient of the William Lawrence Saunders Gold Medal.

This is the first time in the Institute's 86 years that there has been a father and son achievement of so high an award. The younger Hoover, a graduate of Stanford University, received an M.A. from Harvard in 1928, and had a teaching fellowship at California Institute of Technology, of which he later became a Trustee.

Mr. Hoover, Jr., assumed the presidency of Geophysical Corp., Pasadena, Calif., in 1935 and later became chairman of the board. However, he left his business interests to undertake assignments in foreign relations which led to his appointment as Under Secretary of State. He is a member of the American Assn. of Petroleum Geologists and the Society of Exploration Geophysicists.

The award to Herbert Hoover, Jr., will be the 17th in the 27-year-old history of the gold medal.

NOHC Eastern Section Fall Meeting Program

The Warwick Hotel in Philadelphia will be the scene for the Fall Meeting of the National Open Hearth Committee's Eastern Section, on October 12, 1956.

PROGRAM

Morning Session—9:30 am

Chairman—Charles D. Moore, G. & W. H. Corson Inc.

Vice Chairman—George S. Baldwin, Baldwin-Lima-Hamilton Corp.

1. Open Remarks
2. Eastern Section Award Paper
3. Panel Discussion

Moderator—R. Russell Fayles, Lukens Steel Co.

Keith Bartels, U. S. Steel Corp.; Gardner Blythe, Colorado Fuel & Iron Corp.; E. E. Callinan, Alan Wood Steel Co.; James Clisham, Bethlehem Steel Co.; Russell Farr, American Steel & Wire Co.; Richard Stanford, Bethlehem Steel Co.

Lunch—12:15 pm

Afternoon Session—1:30 pm

Co-Chairmen—Harry Davis, Bethlem Steel Co., and Jack Sutherland, U. S. Steel Co.

1. *An Automatic Fuel Control System*
   D. R. Mathews, Alan Wood Steel Co.
2. *Safety Salesmanship*
   Frank Bird, Lukens Steel Co.
3. *Refractories*
   W. S. Debenham, U. S. Steel Corp.
4. *The Use of a Direct Reading Spectrometer in the Open Hearth*
   John Scott, Lukens Steel Co.
5. *Factors Affecting Ingot Quality*
   A. L. Lehman, Bethlehem Steel Co.

Reception—5:30 pm

Dinner—6:30 pm

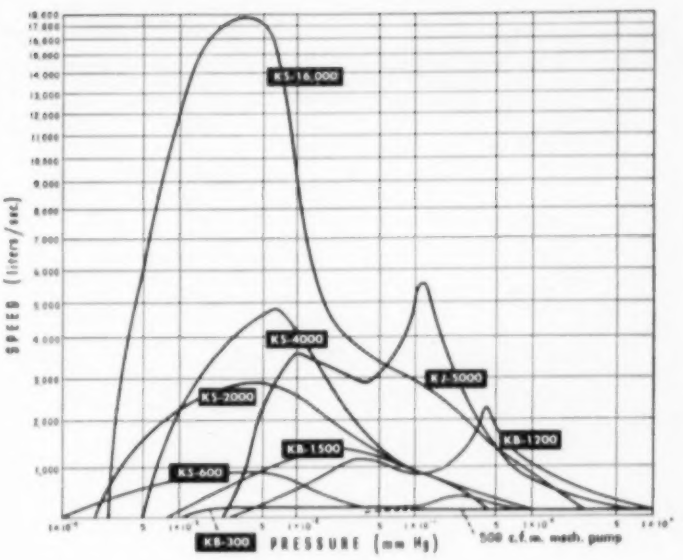
After-Dinner Speaker—
D. R. Loughrey

Entertainment—
Courtesy Suppliers' Committee

IMD Will Publish Three Nuclear Symposium Papers

The three papers to be presented at the Nuclear Metallurgy Symposium during the IMD Fall Meeting at Cleveland, Monday, October 8, are being printed in book form. Copies can be purchased at the registration desk, Hotel Carter, or can be ordered by mail. This is the third volume on nuclear metallurgy published by the Institute of Metals Division.

The papers to be published in this book include: *Theory and Mechanism of Radiation Effects in Metals* by G. H. Vineyard, Brookhaven National Laboratory; *Irradiation Effects on Physical Metallurgical Processes* by D. E. Thomas, Westinghouse Electric Corp.; and *Irradiation Effects in Reactor Materials* by D. S. Billington, Oak Ridge National Laboratory.



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Southwestern Section Of NOHC Announces Annual Meeting Program

The Southwestern Section of the National Open Hearth Committee will hold its annual Globe Trotters' Meeting in Pueblo and Colorado Springs, Colo., September 27 and 28. The host will be the Colorado Fuel & Iron Corp. Arrangements have been made for special cars on the Missouri Pacific Eagle leaving St. Louis Wednesday, September 26, at 4 pm and returning from Colorado Springs on Saturday, September 29, at 5:38 pm. The Friday program will be at the Hotel Broadmoor in Colorado Springs.

Those attending will be wined and dined by C. F. & I. beginning on Thursday with breakfast at the Minnequa Club, and lunch at Pueblo Golf & Country Club. A Men's Luncheon will be held on Friday followed by the Suppliers' Cocktail Party at 5:30 pm winding up with the Fellowship Dinner in the Broadmoor Hotel Ballroom that evening.

Activities for the ladies include a luncheon in the Terrace Lounge, a tour of Colorado Springs and vicinity, and dinner at the Garden of the Gods Club, all on Friday. A special breakfast and short tour will be arranged for any wives who desire to visit C. F. & I.'s plant.

PROGRAM

Technical Sessions

1. *Human Aspects of Shop Production*
   George Grosvenor, C. F. & I.
   Followup—Carl Henzelman, Granite City Steel Co.
2. *Installation of Prefabricated Front Buckstays on O. H. Furnaces*
   Art Schoenebeck, Granite City Steel Co.
3. *Increasing Scrap Density to Reduce Charging Time*
   Steve Purcell, Lone Star Steel Co.
   General Discussion.
4. *Recent Developments in Use of Oxygen—Progress Report*
   Gene Miller, C. F. & I.
5. *Recent Substitution of Basic Brick in O. H. Construction, Including Checkers*
   A. H. Sommer, Keystone Steel & Wire Co.
6. *Mechanical Aids to O. H. Repairs*
   M. H. Weir, Sheffield Steel Co.
   Mechanical Repairs to Open Hearth Furnaces
   Douglas Taylor, Granite City Steel Co.
   Prepared Discussion on Repair Procedures.
   Clarence King, Sheffield Steel Co.
   Levi Yager, Laclede Steel Co.
   Steve Purcell, Lone Star Steel Co.
   A. H. Sommer, Keystone Steel & Wire Co.
   Frank LaRocca, C. F. & I.
7. S. W. Section Award Paper.

1957 AIME Directory To Be Ready in June

To obtain the necessary data for the *1957 AIME Directory*, a two-part business reply card will be sent to all members in the fall of 1956. The Directory, to be published next June, will consist of an alphabetical list of members, date and grade of membership, title, company name and address. It will also include a listing by state, city, and company, somewhat as in the 1950 Directory, instead of a geographical listing solely by individual members as was done in the 1955 Directory. In the geographical section, the member's divisional affiliations will be listed, and also whether he is available for consulting work.

A separate supplement listing of local sections, student chapters, branches and divisions, and committee personnel will also be issued. Printing will be by photo offset. It is proposed, though financial action has not been taken, that distribution will be free to members, but only to those who request it through a coupon to be published in the journals. A considerable saving in cost is thus possible as only about 13 pct of the members ask for a copy. Complete cost of the directory and supplement has been budgeted at $15,000, plus $2,500 for recording new divisional affiliations.

IBM Address System

The Institute plans to install a combination IBM-Addressograph system, to be shared with the AIEE. Initial cost will be about $8,500, plus $2,000 as a writeoff of undepreciated present Addressograph equipment. A net annual saving to the Institute of between $7,000 and $8,000 is expected compared with present expense for similar service.

Stahl und Eisen

The German iron and steel magazine, *Stahl und Eisen*, published in Dusseldorf, celebrated its 75th anniversary in July with a special issue. This technical journal is under the direction of Dr.-Ing. Kurt Thomas, Life Member, AIME. Through the years the magazine has reported the technologic, economic and scientific developments in the industry almost since the beginning of the production of liquid steel.

Some of the featured articles in the anniversary issue are: *Possibilities of increasing the output of ore sintering plants; Effect of small chromium and nickel contents on the properties of steel products; Some thoughts on brittle fracture;* and *Manufacture and operation results of converter bottoms with magnesite tuyeres.*

Steel Founders' Society Offers $6300 in Prizes

The Steel Founders' Society of America is conducting its Third Product Development Contest with a total of $6300 in cash prizes as well as national certificate awards. The Society's aim is to promote greater utilization of cast steel as an engineering material, and to demonstrate recent technological progress, as well as to encourage further progress in the steel foundry industry. Those eligible to participate will be divided into two groups, one consisting of professors, students, engineers and employees of customers of the steel castings industry. The second class will be composed of employees of steel castings producers in U. S., Canada and Mexico.

Equal consideration will be given to entries in the area of original design, utility, theory, replacement, redesign, new items, and actual case histories already in production. Any metal or alloy considered a steel casting or practical for steel foundry production is eligible. Complete contest information may be obtained by writing to: Third Product Development Contest, Steel Founders' Society of America, 606 Terminal Tower, Cleveland 13, Ohio, enclosing name, position, company, address. Contest closes June 1, 1957.

Conference on Magnetism

The second Conference and Exhibit on Magnetism and Magnetic Materials will be held in Boston at the Hotel Statler, October 16-18.

The conference is sponsored by AIEE in cooperation with AIME, IRE, and the American Physical Society. In addition to the program of technical papers, the meeting will feature exhibits by leading manufacturers of magnetic materials and associated equipment. R. M. Bozorth, Bell Telephone Laboratories, Murray Hill, N. J. is Conference Chairman, and C. P. Bean, General Electric Research Laboratory, Schenectady, is Program Chairman.

At the dinner on October 17 at the Museum of Science Dr. Bozorth will speak on his recent trip to Russia to attend a magnetism conference.

Eight sessions will include approximately 75 technical papers on the following topics: Magnetic Anistropy; Permanent Magnets and Fine Particles; Magnetism and Physical Metallurgy; Losses in Soft Magnetic Materials; Ferrites; High Frequency Phenomena; Switching Devices and Magnetic Amplifiers; and Apparatus and Design.

The complete program will be published in the October issue of *Electrical Engineering*. Abstracts of the technical papers will be available before the meeting.

Golden Anniversary To Be Celebrated At Steel Conference

Fifty years of electric furnace steelmaking will be celebrated at AIME's Fourteenth Electric Furnace Steel Conference at the Morrison Hotel, Chicago, December 5-7, 1956. Marking the golden anniversary of the electric arc process for steelmaking will be a paper entitled *Fifty Years of Progress in the Electric Arc Furnace*, by Samuel Arnold, III, Consulting Engineer, Pittsburgh, to be given at the first of the technical sessions on Thursday morning, December 6. Specially honored will be Herman B. Schulz, one of the oldest living electric furnace steelmakers, who will serve as honorary chairman of the session. He was a steelmaker when the first heat was made in an electric arc furnace in 1906.

The three-day Conference will open on Wednesday, December 5, with plant trips to the South Chicago Works of Republic Steel Corp., and the Indiana Harbor Works of American Steel Foundries.

E. R. Johnson, vice president-operations, Republic Steel Corp., will be the principal speaker at the annual dinner in the Ballroom of the Morrison Hotel on Thursday evening, December 6. A reception will precede the dinner.

Other papers to be given at the opening technical session include: *Appraisal of European Electric Furnace Melting* by Robert Boutigny, director of engineering, Stein et Roubaix, Paris, France; *Past and Future Trends of Arc Furnace Transformers as Related to Electric Furnace Growth* by R. Bry, General Electric Co.; and *Cyclical Rises and Declines in the Price of Steel Scrap*, by C. Hoskins, Institute of Trend Research. On Thursday afternoon, December 6, a session will be held on the Solidification of Electric Furnace Steels. Papers at this session will deal with the physical chemical aspects of steelmaking.

On Friday morning, December 7, simultaneous sessions are scheduled on *Developments for Control of Quality, and Interpretation of Results*, and on *Hot Tearing Characteristics of Steel and Ladle Refractories*. On Friday afternoon, there will be a session of interest to specialists on steel castings, describing the use of oxygen in the electric furnace. The other session will hear papers on control of smoke and fume from electric furnaces, and on sources of iron other than scrap. Among the speakers on this latter subject are P. E. Cavanagh, director of the department of metallurgy of the Ontario Research Foundation in Toronto, and Ulf Kalling, manager of the Steel Div., Stora Kopparberg Bergslags located in Falun, Sweden.

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Engineering Societies Personnel Service Inc., held its first joint meeting of the Board of Directors and branch office managers, July 23-24, at the Engineering Societies Building, N.Y.C. Their discussion of ways to meet the changing employment conditions resulted in several important ESPS national policy changes. Pictured at the meeting, left to right, standing; R. Gardner, AIEE; B. Allen, Chicago Manager; G. Parker, N. Y. Manager; J. Decker, San Francisco Manager; O. B. Schier II, ASME; E. J. Kennedy, AIME; P. Apol, AIME; J. A. Zecca, ASCE; E. Hartford, ASME; F. McKinless, Detroit Manager. Seated: E. Kirkendall, AIME Secretary; C. E. Davies, ASME Secretary; N. Hibshman, AIEE Secretary; A. H. Meyer, ESPS Executive Director; and W. Wisely, ASCE Secretary.

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NATIONAL MEMBERSHIP COMMITTEE

BOOKS

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Solid State Physics, Vol. 2: Advances in Research and Applications, edited by Frederick Seitz and David Turnbull, *Academic Press Inc.*, $10.00, 468 pp., 1956.—Second in a series aimed at the comprehensive presentation of recent developments in solid state science, this volume includes articles on nuclear magnetic resonance, electron paramagnetism, neutron diffraction, the theory of specific heats and lattice vibrations, and displacement of atoms during irradiation.

Advances in Applied Mechanics, Vol. 4, edited by H. L. Dryden and T. von Karman, *Academic Press Inc.*, $10.00, 413 pp., 1956.—This is a compilation of seven articles on the turbulent boundary layer, nonlinear elasticity, physical and statistical aspects of fatigue, three-dimensional boundary layer theory, dislocation theory of metal plasticity, the concept of elastic stability, and the Poincaré-Lighthill-Kuo method.

Atoms and Energy, by Prof. H. S. W. Massey, *Philosophical Library*, $4.75, 168 pp., 1956.—A non-technical study of developments in atomic physics with emphasis on the relation of research to atomic energy rather than the application of atomic processes.

Jackling Will Designates AIME to Receive $150,000

Former AIME President Daniel C. Jackling, who died March 13, 1956 at the age of 86, has bequeathed a total of $150,000 to the AIME; $50,000 to be used for scholarships, $75,000 for a new building "in New York", and $25,000 for the Woman's Auxiliary Educational Fund. It has been estimated that the amounts of these bequests may be increased many times when the funds become available; in the meantime a trusteeship has been set up during Mrs. Jackling's lifetime.

Mr. Jackling left an estate valued at several million dollars, most of which will go to educational institutions, churches, and scientific societies. Other substantial bequests included: $500,000 to Mrs. Jackling; some $400,000 to relatives, friends and employees; $100,000 to the University of Missouri; $50,000 to Stanford and to the Red Cross; $75,000 each to the Boy Scouts and Girl Scouts; and $100,000 to the Mormon Church.

PERSONALS

B. MOREELL

Admiral Ben Moreell has been chosen to receive the John Fritz Medal, the joint high award of the AIME, ASCE, ASME and AIEE. Others who have received this award in the past for distinguished services in war and peace, include Bell, Edison, Orville Wright, Marconi, Sperry, and Herbert Hoover, Sr. Presentation of the gold medal and certificate will be made October 17, 1956 in Pittsburgh, during the national convention of ASCE, of which he is an honorary member. Born in Salt Lake City, Ben Moreell graduated from Washington University. He holds numerous honorary doctorate degrees and is Chairman of the Board, Jones & Laughlin Steel Corp., Pittsburgh. Admiral Moreell won recognition in World War II as organizer and commander of the Navy *Seabees*. At 51, he was the youngest Vice Admiral, and also received the Distinguished Service Medal and Gold Star. Admiral Moreell has been chief of Naval Civil Engineers and of the Material Div., Office of the Assistant Secretary of the Navy. A distinguished authority on concrete he has contributed notable technical papers on this subject. Admiral Moreell is past president of American Concrete Institute, the Society of American Military Engineers and the Army and Navy Club.

Fred L. Shanklin has been named president of Union Carbide Ore Co., to succeed **F. S. Haggerson** who resigned for reasons of health.

Walton S. Smith has retired as vice president in charge of manufacturing, Metal & Thermit Corp. He is succeeded by **Bernhard W. Weber** now manager of manufacturing.

Charles M. Offenhauer has been promoted to manager of administration, Metals Research Laboratories, Electro Metallurgical Co., Niagara Falls.

B. W. Kerrigan has been appointed secretary of the Institution of Mining and Metallurgy, beginning this month. An honor student of classics, Mr. Kerrigan received his formal education at Oxford University. By studying in his spare time he was able to pass the final bar examination in 1949. Mr. Kerrigan terminated a teaching career in 1940 to join the R.A.F., and for two years was attached to the Marconi Co.'s Research Station, Essex. He was commissioned in the Education Branch in 1943. Following the war, he joined the staff of the Imperial College of Science and Technology. In 1953 he was named chief assistant to the director of Building Works, in which capacity he has administered much of the extensive scheme for expansion of the College.

Merrit Alvin Williamson has resigned as manager of the research division, The Burroughs Corp., to become dean of the College of Engineering and Architecture, Pennsylvania State University. No neophyte scholar, Dr. Williamson holds five degrees from Yale, California Institute of Technology, and the University of Chicago. His record in industry has been equally formidable during the 19 years since his commencement, including work with Scovill Mfg. Co., Solar Aircraft Co., and Pullman Standard Car Mfg. Co., where he was associate director of development. Dr. Williamson holds membership in countless scientific societies and professional organizations including AIME, ASTM, ASM, and has published numerous articles in the fields of rockets, metallurgy, and materials.

T. E. TIETZ

Thomas E. Tietz heads the physical metallurgy group, and **William R. Holman** that of liquid metallurgy in the newly formed Dept. of Metallurgy at the Stanford Research Institute, Menlo Park, Calif.

Charles W. Briggs addressed the 23rd International Foundry Congress held in Dusseldorf, Germany, September 6. He spoke on *Green Sand Molding of Large Steel Castings*. A recognized authority in the field of steel castings, Mr. Briggs has been technical and research director of the Steel Founders' Society of America since 1938, and consultant at the Naval Research Laboratory, Washington, D. C. His itinerary includes visits to steel foundries in England, France, and Switzerland, as well as those in the host country, West Germany.

Joseph K. Stone, Jr. has joined Kaiser Engineers, Oakland, Calif. He was a research associate with U. S. Steel Corp. in Pittsburgh.

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Marvin J. Udy has been awarded an honorary Doctor of Science degree by Alfred University, in recognition of his achievements in the field of electric furnace research. Dr. Udy, a graduate of the University of Utah, is an associate and consultant of Stratmat Ltd., and stockholder in Strategic-Udy Metallurgical & Chemical Processes Ltd. An AIME member since 1918, he also holds membership in AICE, ASM, ACS, Electrochemical Society, American Society for the Advancement of Science, and Institute of Chemists.

N. C. FICK

Nathaniel C. Fick has joined the President's Executive force as Program Expeditor in the Office of Defense Mobilization, Wash., D. C. Mr. Fick is chairman of the ASM Washington Chapter, and a member of the EMD, AIME National Executive Committee.

K. H. ROLL

Kempton H. Roll is executive secretary and treasurer, Metal Powder Assn., now located at 130 W. 42 St., N. Y. C. At the time of his appointment to this newly created post, Mr. Roll was technical director of both MPA and the Lead Industries Assn. He has also served as general chairman of the 1956 International Corrosion Conference, and is currently chairman of the N. Y. section of ASM and the AIME Powder Metallurgy Group in New York.

Stanislaus Skowronski, far right, accepts silver tray from Anaconda representatives including Merrill Mosher, far left, at the testimonial dinner marking his retirement, which was held in the Essex House, Newark, N. J.

Merrill A. Mosher has been promoted from assistant general superintendent, Raritan Copper Works to general superintendent. He succeeds **J. P. Cooper,** who moves up to the position of assistant to the manager. Mr. Cooper is a graduate of Lehigh University, while Mr. Mosher claims Cornell as his alma mater. Both men have been with Raritan Works for more than 40 years. Also included in key promotions at Raritan are **John M. Casteras,** who becomes assistant general superintendent, and **C. D. Pearce,** metallurgist and chief research engineer.

ALCOA has announced some changes in their fabricating division staff. **T. F. McCormick** is assistant chief metallurgist; **W. E. King,** chief development metallurgist; and **K. B. Baker,** chief control metallurgist—all at the Pittsburgh office.

G. Keith Allen has been elected president of the Institution of Mining and Metallurgy for the 1957-58 term. Born and educated in Australia, Mr. Allen received the B.M.E. degree from Melbourne University in 1921. Much of his mining experience was gained in Africa, where he started in 1922 with the Rhodesia Broken Hill Development Co. Ltd. He left Africa in 1932 for the London offices of British South Africa Co. Ltd., and in 1935 became assistant consulting engineer of the West African Gold Corp. Mr. Allen became resident director and chief engineer for that company in 1949, and has been spending eight months of each year on the Gold Coast. His service with the Institution began in 1942, and he has held office as vice president for the sessions 1946-49, and also during the current session.

Kalevi V. Kiukkola, formerly research engineer for Husquarna Vapenfabriks AB, Sweden, is now at the copper smelter of Outokumpu Oy in Harjavalta, Finland.

Stanislaus Skowronski has retired from his post as research engineer, Raritan Copper Works, The Anaconda Co., a subsidiary of International Smelting & Refining Co., Perth Amboy, N. J. He had been with the company since 1912. Past-chairman, IMD, he was also vice president of the Electrochemical Society, chairman of ASM in N. J., founder and president of the Technical Societies Council of New Jersey, and a member of ACS. Mr. Skowronski has written many articles on copper and lectured on this topic before technical and civic groups. For 25 years he was adjunct professor of metallurgy at Brooklyn Polytechnic Institute.

K. M. WEIGERT

Karl M. Weigert is conducting brazing research in the new laboratories of the Curtiss-Wright Corp., Quehanna, Pa. Dr. Weigert was research metallurgist with Goldsmith Bros. Smelting & Refining Co.

Norman B. Obbard has been appointed assistant executive vice president—operations, U. S. Steel Corp. He will be succeeded as president of the corporation's American Bridge Div. by **Austin J. Paddock.**

Welding Conference Set For Boston in September

The Boston Section of the American Welding Society is sponsoring an all day New England Regional Conference on Welding on September 19, 1956 at the New England Mutual Hall in Boston. Six technical papers of the latest advances in welding theory and practices will be presented by nationally prominent speakers in the welding field. The technical sessions will be followed by a social hour and banquet.

OBITUARIES

Oscar E. Harder (Member 1918) died of a heart attack July 10, 1956, at the age of 73. A nationally known metals scientist, Dr. Harder was consulting metallurgist to Battelle Memorial Institute since his retirement as assistant director in 1949. Born in Franklin County, Ark., he received his B.A. and M.A. degrees from the University of Oklahoma and later obtained a Ph.D. at the University of Illinois. His field of specialization involved research on precious metal alloys, steel castings, and heat treatment of metals. Dr. Harder's long and active career included teaching at the University of Minnesota where he was professor of metallography; he was food analyst for the Kansas State Board of Health, and research chemist for ASTM and the Portland Cement Assn. In 1928 he was awarded the Howe Medal by ASM which he had served as president, vice president and section chairman. He was also president of the ACS Minnesota Section, and a member of Phi Beta Kappa. The author of numerous technical papers, articles and a book on dental metallography, Dr. Harder held more than 100 patents on steels and alloys from 20 governments, including France, Norway and Hungary.

Erle G. Hill (Member 1928) died July 26, 1956. Dr. Hill had been a professor at Carnegie Institute of Technology and the University of Pittsburgh. At the time of his death he was retired director of metallurgy and development, Wheeling Steel Corp., in W. Va. Born 1890 in Santa Ana, Calif., he studied mining and metallurgical engineering at the University of California where he also did graduate work. Dr. Hill held a variety of posts including a research fellowship at Mellon Institute; he was research director, Lukens Steel Co., and assistant to the general superintendent, Carnegie Illinois Steel Co. at Gary, Ind. ISD Chairman in 1945, Dr. Hill was also chairman of the NOHC and a member of ASM, ASTM and the Franklin Institute.

William McCombs Frailey (Member 1955) died March 10, 1956. A native of Pittsburgh where he was born in 1905, Mr. Frailey was employed as the master mechanic for the Hidden Splendor Co., Greenriver, Utah, at the time of his death. Prior to that he had been maintenance engineer at Pioche Manganese Co., Caselton Pioche, Nev. From 1950 to 1953 Mr. Frailey worked as a mechanical engineer on mill construction in Guatemala.

Rudolph M. Lombardi (Member 1951) died May 12, 1946, at the age of 34. A native of Philadelphia, Mr. Lombardi was plant metallurgist, Colorado Fuel & Iron Corp., Claymont, Del. at the time of his death. A veteran of four years' service in the U. S. Army Air Force, he entered the University of Pittsburgh in 1946 and received his B.S. in 1949. Mr. Lombardi was a member of ASM and the Franklin Institute of Philadelphia, and director of the Acid Open Hearth Research Association Inc.

Laurance I. Neale (Member 1914) died Mar. 31, 1956. Mr. Neale was a minister at The Unitarian Church of All Souls, Lexington Ave., New York. He was born in Boston in 1885. After graduating from Harvard University in 1906 he was employed by George A. Fuller & Co., and later by J. B. King & Co., New York. In 1924 he joined U. S. Gypsum Co., where he organized and managed two new departments. From 1927 to 1936 Mr. Neale was with Atlantic Gypsum Products Co., New York. He was vice president and a director of the Gypsum Assn. from 1929 to 1936.

NECROLOGY

Date Elected	Name	Date of Death
1929	Walter M. Briggs	Unknown
1906	W. R. Dunn	Unknown
1955	William M. Frailey	Mar. 10, 1956
1918	Oscar E. Harder	July 10, 1956
1928	Erle G. Hill	July 26, 1956
1951	Rudolph M. Lombardi	May 12, 1956
1944	James D. McClintock	July 2, 1956
1916	A. J. McNab	July 4, 1956
1928	A. H. Pogson (Rocky Mountain Member)	Unknown
1947	Charles H. Snelson	Unknown

MEMBERSHIP

Proposed for Membership
Metals Branch, AIME

Total AIME membership on August 31, 1956 was 25,451; in addition 2,839 Student Associates were enrolled.

ADMISSIONS COMMITTEE

R. B. Caples, Chairman; F. A. Ayer, Vice-Chairman; A. C. Brinker, R. H. Dickson, C. R. Dodson, R. B. Fulton, T. D. Jones, F. W. Hanson, Sidney Rolle, F. T. Sisco, O. B. J. Fraser, F. W. McQuiston, Jr., A. R. Lytle, L. P. Warriner.

The Institute desires to extend its privileges to every person to whom it can be of service, but does not desire as members persons who are unqualified. Institute members are urged to review this list as soon as possible and immediately to inform the Secretary's office if names of people are found who are known to be unqualified for AIME membership.

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COMING EVENTS

Sept. 16-23, ASTM, Pacific Coast meeting, Statler Hotel, Los Angeles.

Sept. 19, Amer. Welding Soc., Boston Section, all-day New England regional conference on welding, New England Mutual Hall, Boston.

Sept. 26-28, AIME, Rocky Mountain Minerals Conference, Newhouse Hotel, Salt Lake City.

Sept. 27-28, AIME, NOHC Southwestern Local Section, annual fall get together and plant trip. Sept. 27, trip to Colorado Fuel & Iron Co., Pueblo, Colo. Sept. 28, technical sessions, Broadmoor Hotel, Colorado Springs, Colo.

Oct. 1, 1956, AIME, NOHC, Chicago Local Section, Fall Meeting, Phil Smidt's Restaurant.

Oct. 5, AIME, NOHC Southern Ohio Local Section, annual conference, Deshler-Hilton Hotel, Columbus, Ohio.

Oct. 8-10, AIME, IMD Fall Meeting, Hotel Carter, Cleveland.

Oct. 8-12, ASM, national metal congress and exposition, Cleveland Public Auditorium, Cleveland.

Oct. 12, AIME, NOHC Eastern Local Section, annual fall meeting.

Oct. 14-17, AIME, Petroleum Branch, Biltmore Hotel, Los Angeles.

Oct. 16-18, AIEE, AIME, IRE, and Amer. Physical Soc., conference on magnetism and magnetic materials, Hotel Statler, Boston.

Oct. 22-24, ASA, annual meeting, Hotel Roosevelt, New York.

Oct. 25, 26, AIME-ASME, Joint Solid Fuels Conference, Sheraton-Park Hotel, Washington, D. C.

Oct. 28-Nov. 1, Electrochemical Soc., Statler Hotel, Cleveland.

Nov. 2, AIME, Pittsburgh Local Section, NOHC Pittsburgh Local Section, annual off-the-record meeting, William Penn Hotel, Pittsburgh.

Nov. 8-10, AIME, Mining Branch, Northeast Regional Meeting, Hotel Hershey, Hershey, Pa.

Nov. 13, AIME, NOHC Buffalo Local Section, annual meeting.

Dec. 5-7, AIME, Electric Furnace Steel Conference, Hotel Morrison, Chicago.

Dec. 9-12, AIChE, annual meeting, Statler Hotel, Boston.

Feb. 24-28, 1957, AIME Annual Meeting, Roosevelt and Jung Hotels, New Orleans.

Mar. 10-16, 1957, EJC Second Nuclear Engineering and Science Congress, Convention Hall, Philadelphia.

Mar. 25-29, 1957, ASM And Others, Tenth Western Metal Exposition and Congress, Pan-Pacific Auditorium and Ambassador, Los Angeles.

Apr. 8-10, 1957, AIME National Open Hearth Steel and Blast Furnace, Coke Oven, and Raw Materials Conferences, William Penn Hotel, Pittsburgh.

Apr. 11-13, 1957, AIME, Pacific Northwest Regional Conference, Portland, Ore.

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Frankel Co., Inc. *Simons-Michelson Co.*	*
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Great Lakes Carbon Corp. *Davis, Parsons & Strohmeier, Inc.*	1146
International Nickel Co. *Marschalk & Pratt*	1138
Jarrell-Ash Co. *F. P. Walther Jr. and Assoc.*	1204
Jet Propulsion Laboratory, Caltech *Frank Barrett Cole, Adv.*	*
Leitz, Inc., E. *L. W. Frohlich & Co., Inc.*	*
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Morgan Construction Co. *Davis Press, Inc., The*	*
National Research Corp. *Sutherland-Abbott Adv.*	Second Cover
North American Philips Co. *Anders Associates Adv.*	1142, 1143
Pittsburgh Lectromelt Furnace Corp. *The Griswold-Eshleman Co.*	Third Cover
Ramtite Co., The *Marsteller, Rickard, Gebhardt and Reed Inc.*	*
Shieldalloy Corp. *Harold Schor Assoc.*	*
Smeeth-Harwood Co. *Allbright Associates Adv.*	1209
Smidth & Co., F. L. *The Stuart Co.*	*
Steel City Testing Machines, Inc. *Whipple & Black Adv. Co.*	*
Stokes Machine Co., F. J. *The Aitkin-Kynett Co.*	1134, 1135
Swindell-Dressler Corp. *Downing Industrial Adv., Inc.*	*
Titanium Alloy Mfg. Div., National Lead Co. *Comstock & Co.*	1140
Unitron Instrument Div., United Scientific Co. *Price, Tanner & Willox, Inc.*	1211
United States Graphite Co., The	Fourth Cover
U. S. Steel Corp. Columbia-Geneva Steel Div. Tennessee Coal & Iron Div. U. S. Steel Supply Div. U. S. Steel Export Co.	1130, 1131
Utica Drop Forge and Tool Corp. *Wilson, Haight, Welch & Grover, Inc.*	1133
Whittaker, Clark & Daniels, Inc. *Charles W. Curts Adv.*	*
Wiley & Sons, Inc., John *Waterston and Frisch, Inc.*	*
Wyman-Gordon Co. *John W. Odlin Co., Inc.*	1144

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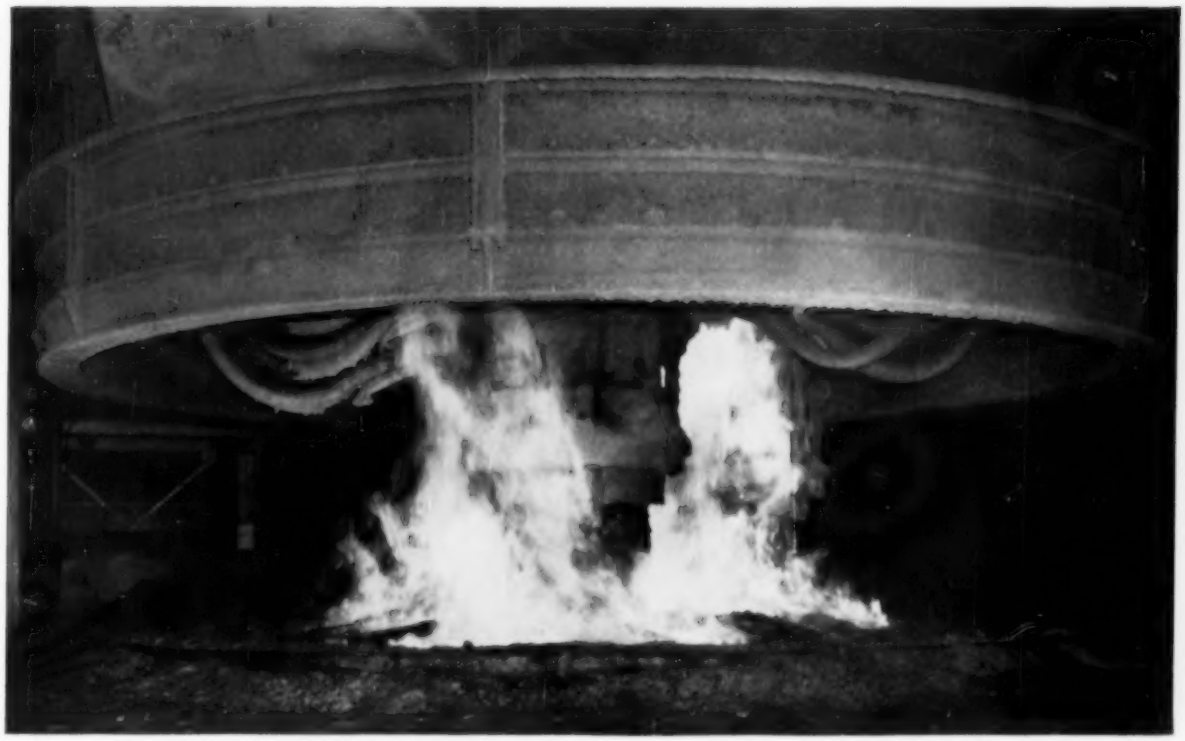

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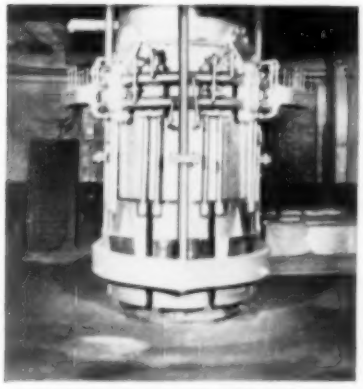

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